AFWAL-TR-80-2022

LEVEL II

SAMARIUM COBALT (SmCo) GENERATOR/ENGINE INTEGRATION STUDY

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GENERAL ELECTRIC COMPANY
CINCINNATI, OHIO 45215

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Final Report for Period August 1977-September 1979

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AIR FORCE WRIGHT AERONAUTICAL LABORATORIES
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This technical report has been reviewed and is approved for publication.

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This study consists of integrating a generator/starter internally on the engine rotor shaft, providing both secondary electric power and engine starting. The integrated engine generator/starter (IEG/S) has been analyzed and conceptually designed for three power levels and three engine categories. The preliminary layouts and supporting analysis of the rare earth, permanent magnet machine indicate that the IEG/S concept is a technically feasible approach to secondary power extraction and engine starting.

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PREFACE

The work described herein was performed by the General Electric Company, Advanced Engineering and Technology Programs Department, Aircraft Engine Group, Evendale, Ohio, under the sponsorship of the Air Force Aero-Propulsion Laboratory and under Contract F33615-77-C-2018.

The study consisted of integrating a generator/starter configuration onto the engine rotor shaft in such a manner as to provide both secondary electric power and engine starting capabilities. The integrated engine generator/starter (IEG/S) was then analyzed and conceptually designed for three power levels and three engine categories. Study results support the supposition that a rare earth, permanent magnet machine in an IEG/S concept is a technically feasible approach to secondary power extraction and engine starting. Use of these study results would be appropriate for inclusion in a future all-electric aircraft configuration.

Significant assistance during this study was provided by Air Force personnel from the Aero-Propulsion Laboratory at WPAFB, Ohio. Both 1st Lt. Philip G. Gaberdiel and William Borger are noted for their guidance and contributions to the successful completion of this study.

Dr. Eike Richter from General Electric's Corporate Research and Development Laboratories and Mr. Charles Triebel and Mr. Robert Webb from GE's Aircraft Equipment Division provided the technical definition and sizing/design of the Samarium Cobalt generator configurations. These were then integrated into various engine configurations at GE-Evendale's Aircraft Engine Engineering Division by Technical Manager Herbert Demel. Mr. Max Baumgardner was Program Manager.

TABLE OF CONTENTS

TABLE OF CONTENTS (Continued)

TABLE OF CONTENTS (Continued)

TABLE OF CONTENTS (Concluded)

LIST OF ILLUSTRATIONS

LIST OF ILLUSTRATIONS (Continued)

LIST OF ILLUSTRATIONS (Concluded)

LIST OF TABLES

LIST OF TABLES (Continued)

LIST OF TABLES (Concluded)

LIST OF SYMBOLS AND ABBREVIATIONS

A/C	-	Aircraft
AGB	-	Auxiliary Gearbox
APU	-	Auxiliary Power Unit
AR	-	Armature Reactance
B, Bg	-	Flux Density
Br	-	Remanence Flux Density
C	-	Cylindrical
C/GG	-	Casing/Gas Generator
D	-	Disk
DS	-	Shaft Diameter
DT	-	Stator Outside Diameter
Ed	-	Energy Density
GCU	-	Generator Control Unit
GG	-	Gas Generator
H	-	Demagnetizing Field
HI	-	Stack Length
HPT	-	High Pressure Turbine
IEG/S	-	Integrated Engine Generator/ Starter
JAN-TX	-	Joint Army-Navy Extra Testing
KG	-	Kilogauss
KL	-	Kiloline
KVA	-	Kilovolt-Ampere
LCC	-	Life Cycle Cost
LPT	-	Low Pressure Turbine
LRU	-	Line Replacement Unit
MGOe	-	Megagauss-Oersted
MTBF	-	Mean Time Between Failures
μ_r	-	Recoil Permeability
PF	-	Power Factor
PM	-	Permanent Magnet
PTO	-	Power, Takeoff
p.u.	-	Per Unit
RMS	-	Root Mean Square
RPV	-	Remote-Piloted Vehicle

LIST OF SYMBOLS AND ABBREVIATIONS - Concluded

S.L.	-	Sea Level
SmCo	-	Samarium Cobalt
SPS	-	Secondary Power System
TGB	-	Transfer Gearbox
UER	-	Unscheduled Engine Removal(s)
V.R.	-	Voltage Regulator
VSCF	-	Variable Speed Constant Frequency
W	-	Watt
WT	-	Weight
Xd	-	Commutating Reactance

SUMMARY

A study program to evaluate the application of integrated engine generator/starter (IEG/S) concepts was conducted to identify the feasibility of integrating a rare earth Samarium Cobalt (SmCo) permanent magnet generator/starter to the aircraft engine rotor. The IEG/S concept extracts electric power from the prime propulsion engine and also provides starting (motoring) of the engine.

The objective of this study included the evaluation of the IEG/S system in terms of payoff potential for the engine system. A major objective was to determine the feasibility and applicability of the IEG/S to typical engines. This objective addressed detail layout, identifying location, configuration, and interfaces of the IEG/S system.

The final results of this study conclude that the IEG/S concept is technically feasible and offers potential advantages over today's typical secondary power systems (SPS). The primary potential advantages of the IEG/S compared to conventional SPS configurations are:

- higher reliability
- less maintenance
- lower life cycle cost

The overall payoff of the IEG/S system is influenced by many factors outside the control of the engine and IEG/S system design. Therefore, a study program is recommended that would evaluate the IEG/S benefits and detriments to the total aircraft system. The goal of the study would be to establish the overall payoff and the effects on the performance of advanced military aircraft with respect to mission requirements and economical considerations, with the IEG/S utilizing a maximum of electrical power supply for functions such as power actuation and environmental control.

The advantages of the IEG/S are most significant at Power Levels III and IIIA, because these levels allow the entire mechanical accessory drive system to be eliminated. (For Power Level III - the most powerful level - the IEG/S is to supply all secondary aircraft and engine power, including power normally used for hydraulic pumps and power supplied by bleed air. For Power Level IIIA the IEG/S is to supply all secondary power except that normally supplied in the form of bleed air.)

With the elimination of the accessory drive system considerable advantages of engine maintainability are achieved through a "clean" engine periphery without the typical congested area around the accessory gearbox (AGB) with its accessories, ducts, and pipings. Electric-motor-driven engine accessories allow strategic accessory location for improved service tasks. The variable-speed-controlled permanent magnet (PM) motor driving a fixed-displacement fuel pump supplies fuel proportional to engine requirements without the bypass losses that are incurred when using conventional bypass-controlled fuel delivery systems, thereby improving pumping efficiency. Higher pumping efficiency keeps the fuel temperature rise low and improves the heat removal capacity of the fuel from the engine lube system.

The reliability of the IEG/S inside the engine is most important for the successful implementation of this design and must be considerably better than present state-of-the-art aircraft generators. The solid-rotor permanent magnet generator/starter is mounted directly on the high-pressure (HP) shaft in combination with a pure electric safety disconnect device (no moving parts), making it possible to achieve a mean time between failures (MTBF) of 50,000 hours. This IEG/S system represents a potential viable candidate for future engine integration. Since the generator-rotor and the electrical safety disconnect have no wearing parts, the chosen IEG/S design requires no periodic maintenance actions as opposed to conventional secondary power systems.

The need for IEG/S accessibility is not a major consideration due to the high system reliability and the absence of periodic maintenance requirements. Replacement of IEG/S components, therefore, could be accomplished during normal engine maintenance.

Although a life cycle cost (LCC) assessment was beyond the scope of this study, lower operational costs can be predicted based on the potential lower maintenance and replacement expenses that would follow from the superior reliability of the IEG/S compared to conventional SPS power generating equipment.

The weight of the IEG/S system compared to conventional secondary power systems does not provide weight advantages for the engine system. However, only the overall SPS aircraft system weight with associated distribution systems (hydraulic, pneumatic, and electric versus an all-electric power distribution), together with end-user equipment, will determine the system weight payoff or penalty compared to conventional aircraft systems.

The major consideration of SPS efficiency is not engine performance but heat dissipation, which influences the selection of available cooling media and cooling equipment weight. The overall IEG/S extraction efficiency depends largely on the power level and engine size.

The IEG/S concept has its highest potential payoff in the application of low bypass engines for high performance aircraft of near sonic or supersonic operation. The elimination of the AGB from this type of engine usually results in a frontal area reduction of fuselage or nacelle, thereby reducing drag and improving the overall aircraft performance. The frontal area drag reduction for high bypass turbojets, however, can also be achieved by taking advantage of alternate AGB locations aft of the fan frame between fan bypass flowpath and compressor case, rather than outboard of the fan case.

This study demonstrates the IEG/S's technical feasibility and good potential for high performance aircraft where the frontal area of the engine installation (nacelle or fuselage) is influenced by the AGB package, as is the case in low bypass or pure-jet engines. The minimum IEG/S electric power generation rating for maximum benefits must be large enough to eliminate the mechanical accessory drive system for aircraft and engine accessories and provide starting of the engine.

The application of the IEG/S concept in dual-spool high bypass turbofans, however, does not show a payoff if frontal area and aerodynamic drag may be reduced by other means. (For example, by having a core-mounted gearbox drive a PM generator/starter.) The applicability of the IEG/S to specific engines has to be studied for each individual case.

SECTION I
INTRODUCTION

Air Force and Industry have recognized the potential value of integrating secondary-power-generation devices and engine accessory components into the basic engine structure. The purpose of this design approach would be to minimize the propulsion system envelope. Provided that a suitable configuration could be defined, the propulsion system should then provide improved installed performance, in two respects: drag would be reduced, and reliability would be improved by doing away with the complex mechanical secondary-power-generating equipment and accessory drive system.

The Aircraft Engine Group (AEG) of the General Electric Company has been funded by the Aerospace Power Division of the Air Force Aero Propulsion Laboratory to perform a Samarium Cobalt Generator/Engine Integration Study Program, Contract Number F33615-77-C-2018. The program encompassed a detailed study and design effort to determine applicability, location, and configuration of a permanent magnet (PM) generator/starter suitable for integration into an engine structure. Parametric and design studies and a design layout of a single selected concept have been completed and are reported herein.

SECTION II

TECHNOLOGY BASE

The groundwork leading to the Permanent Magnet IEG (IEG/S) is quite extensive and is a culmination of three separate technology areas.

A. VARIABLE-SPEED CONSTANT-FREQUENCY POWER SYSTEM

Variable Speed Constant Frequency (VSCF)-type aircraft power systems became technically feasible in the late 1950's and early 1960's with the inception of high-power semiconductor devices. The technology progressed extremely slowly until 1972, when VSCF-type equipment was called upon to solve an A-4 electrical system problem. The excellent results achieved in the A-4 program, coupled with the high life cycle costs (LCC's) of present hydromechanical-drive electrical systems, have led to the selection of the VSCF technology on the new Navy Advanced Fighter Aircraft (F-18). The Air Force is actively pursuing the VSCF concept by Contract F04606-76-C-0902, which has been awarded to The Boeing Company. The objectives of the contract are to procure and install a 60 kilovolt-amp (KVA) VSCF system (wire-wound-rotor design) for service test in the KC-135 aircraft. Additional wire-wound VSCF work was accomplished under Navy Contract N00421-72-C-6579, which was awarded to the General Electric Company for demonstration of a starter/generator VSCF system for the A-6 aircraft. The results of this program showed that VSCF equipment can provide flexible engine-starting systems.

B. RARE EARTH/TRANSITION METAL MAGNETS

The listing of Rare Earth/Transition Metal permanent magnets dates to the 1917 time period. It was not until 1966, however, that a rare earth cobalt-based "family" of super magnets was predicted. During the 1966 to 1973 time period, the Air Force Materials Laboratory played a major role in the development of high-energy rare earth magnets. By 1973, magnets with a 20 million gauss-oersteds energy product were available on the commercial market. It was then apparent that rotating machinery using rare earth transition metal magnets could yield exciting payoffs in weight, volume, and reliability in the 400 Hz aircraft electrical power area. A joint AFML/AFAPL effort was initiated via contract to

the General Electric Company to construct a VSCF starter/generator system using rare earth transition metal magnets in the rotor of the machine. The system size is 150 KVA, 115 volts, 3 phase, 400 Hz, and consists of a solid-rotor synchronous machine together with a solid-state cycloconverter which is used to control power flow in both directions. The operation of this system was successfully demonstrated in early 1978.

The results of this program demonstrated that rare earth magnets could be applied to high-powered 400 Hz electrical starter/generator systems and yield a significant improvement in electrical generating system efficiency. The success of the 150 KVA PM effort resulted in a major follow-on program with 60 KVA starter/generator units which are to be flight-tested on the A-10 aircraft. This program was initiated via contract award (F33615-78-C-2200) to the General Electric Company in August 1978 and will culminate in an extensive one-year service test beginning in 1982.

C. INTEGRATED ENGINE GENERATOR STUDIES

Early in the 1970's the National Aeronautics & Space Administration recognized the need for alternate secondary-power systems for advanced transport aircraft. A contract (NAS1-10893) was awarded to the Boeing Company to study advanced secondary-power extraction techniques. The study program indicated significant technical and economic payoffs (depending upon aircraft configuration) by using the IEG (IEG/S) approach. In this same time period, the Air Force Aero Propulsion Laboratory also investigated Advanced Accessory Drive Systems for turbine engines via a study contract (F33615-72-1170) with Pratt & Whitney Aircraft. Results of the study heavily favor the IEG (IEG/S) in an Advanced Accessory System. The first attempt to manufacture hardware for the IEG concept was also made by Pratt & Whitney via a subcontract to the Bendix Corporation. This effort was done for the Naval Air Propulsion Test Center under contract N00140-73-C-0126. A brushless 30 KVA, 3600 to 11,100 rpm, VSCF-type generator was successfully designed, built, and tested in a Pratt & Whitney J52 engine environment.

SECTION III
PROGRAM OBJECTIVES

The objectives of this study program are to investigate, analyze, and define the applicability, optimal location, and configuration of rare earth SmCo permanent magnet (PM) generator/starters suitable for integration into the main rotor system of typical aircraft gas turbine engines. A major objective is the definition of the engine interface for such an integration.

The configuration study and design entails the conceptual design of a matrix of three engine classes and three or more power levels as defined in Section IV. In addition to establishing the functional and performance feasibility of this generator/engine integration concept, consideration shall be given to aspects such as maintenance, reliability, and system safety. Specific safety design objectives include the incorporation of a safety disconnect to protect the engine in the event of an internal PM machine fault or a feeder fault without altering normal engine operation.

A parametric tradeoff study will result in the selection and identification of a single engine/power level combination most suited to demonstrate the Integrated Engine Generator/Starter (IEG/S) technology for further in-depth analysis and detail design layout. The advantages being sought from the generator/engine integration include improvements in both engine frontal area (elimination or size reduction of the accessory gearbox), reduction in weight, and improved reliability over the baseline engine and secondary power generation system.

SECTION IV

ENGINE CLASSES AND GENERATOR POWER LEVELS STUDIED

The study considered three engine classes and assessed three levels of electrical power requirements for each engine class.

The three specified engine classes are categorized as follows:

- Low Bypass Turbofan
- High Bypass Turbofan
- Small Turbofan for Remote Piloted Vehicle (RPV)

The initial definition of power level requirements is:

- I Generation of electric customer power
- II Generation of electric customer power plus electric engine starting
- III All electric secondary power extraction (including bleed air) plus electric starting

A fourth power level has been introduced by GE during the program to broaden the IEG/S application possibilities, defined as:

- IIIA Same as III, except that customer bleed air is provided by the engine compressor.

A. ENGINE CLASSES

The following General Electric production engines match the specified characteristics of engine classes:

Low Bypass Turbofan:	F404
High Bypass Turbofan:	F103 (CF6-50)
Small Turbofan (RPV):	TF34

Pertinent performance characteristics and applications of those engines are discussed below, along with discussions of how beneficially each engine can be applied toward the IEG/S concept.

1. Low Bypass Turbofan Engine

The F404 is a low bypass, augmented turbofan engine developed for application in advanced fighter aircraft (F-18). This type of engine benefits most from the advantages of a minimum frontal engine area and associated drag reduction in high performance aircraft (A/C) near or above sonic speed.

The elimination or reduction of the peripheral case-mounted accessory gearbox (AGB) directly influences the total frontal area of the F404, an engine whose shape approaches that of a straight cylinder. The F404 engine configuration and specification are shown in Figure 1.

Although the IEG/S payoff potential of a low bypass turbofan or jet engine is high, the PM machine design integration is difficult because of the small physical space available.

Engine cross sections of the F404 can be found in Addendum A, Drawings, of this report.

2. High Bypass Turbofan Engine

The F103 is the military designation for the CF6-50 high bypass commercial turbofan. This engine has applications in commercial A/C (Airbus A300B, Douglas DC-10, Boeing 747) and Advanced Military Transport (USAF ATCA-Tanker (DC-10)) and USAF E-4A/B-Command Post (747).

The F103 has a fan-case-mounted accessory gearbox, adding directly to the frontal projection of the engine and nacelle area in a wing-pod-mounted installation. Advanced versions of this family of engines will be supplied with core-mounted gearboxes that do not affect engine frontal area.

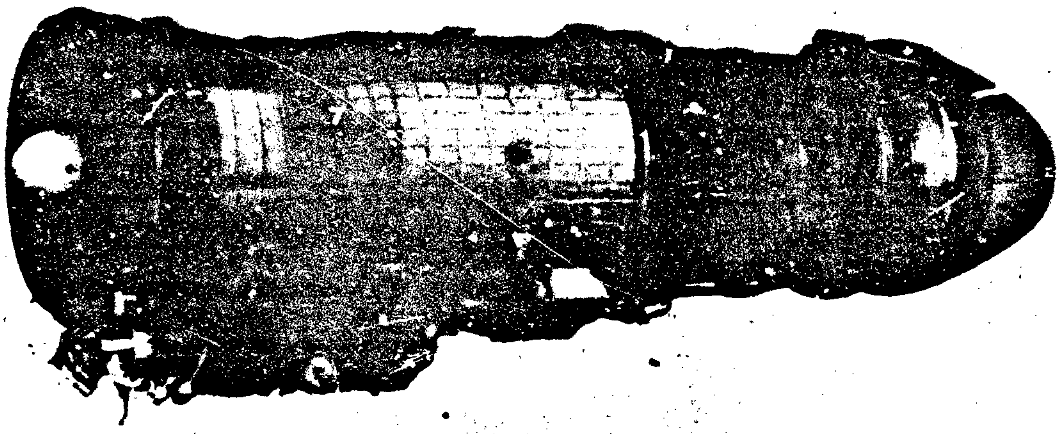
The F103 engine configuration and specifications are shown in Figure 2.

High bypass turbofans have a large space available in the forward sump (between fan and compressor) for IEG/S installation, making it possible for an IEG/S to be installed without major modification.

Engine cross sections of the F103 can be found in Addendum A, Drawings.

3. Small Turbofan Engine

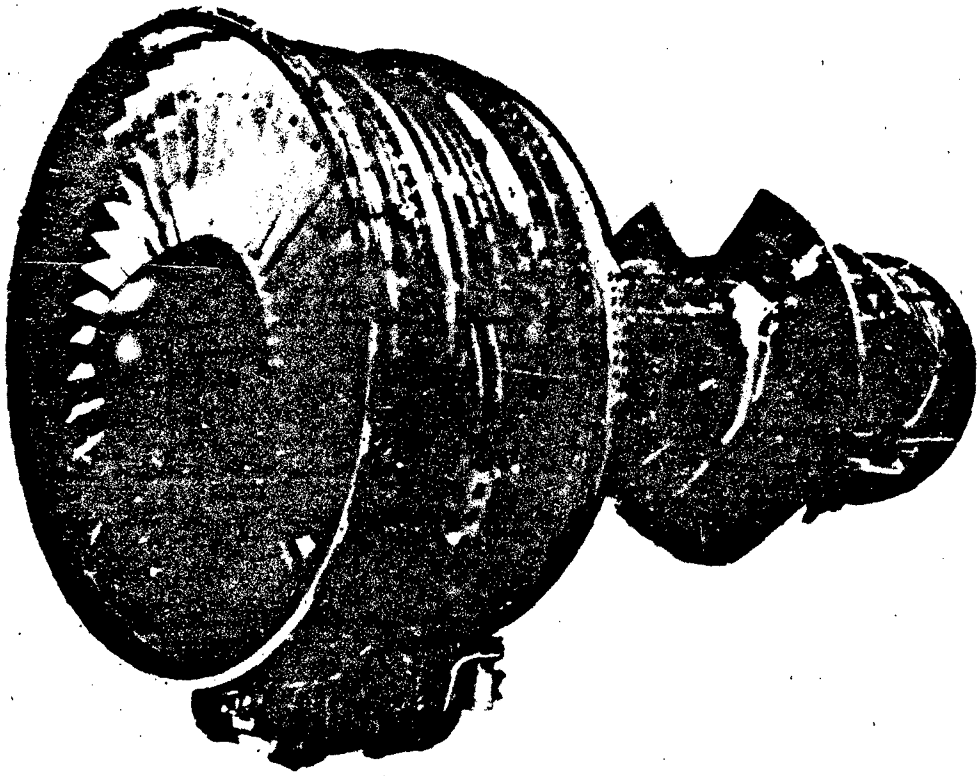
The TF34 is a high bypass turbofan engine selected for Remote Piloted Vehicle operation. This engine has applications for the advanced USAF A-10 Attack Aircraft and the Navy's S-3A Anti-Sub Aircraft. The TF34 incorporates a core-mounted accessory gearbox located between core and fan flowpath, where it does not add to frontal engine area. The TF34 engine configuration and



SPECIFICATIONS

Weight, lb	2120
Length, in.	159
Maximum Diameter, in.	35
Fan/Compressor Stages	3/7
HPT/LPT Stages	1/1
Bypass Ratio	0.30
Pressure Ratio	25
Airflow, lb/sec	144
Maximum Thrust SLS, lb	10,600 (16,000 Augmented)
SFC	0.80 (1.86)
Thrust/Weight	5 (7.5)

Figure 1. F404 Engine.



SPECIFICATIONS

Weight, lb	8375
Length, in.	190
Maximum Diameter, in.	86.4 (Fan)
Fan-(Booster)/Compressor Stages	1-(3)/14
HPT/LPT Stages	2/4
Bypass Ratio	4.4
Pressure Ratio	30.3
Airflow, lb/sec	1484
Maximum Thrust SLS, lb	52,500
SFC	0.393
Thrust/Weight	6.3

Figure 2. F103 Engine.

specifications are shown in Figure 3. Engine cross sections of the TF34 can be found in Addendum A, Drawings.

B. ELECTRICAL SYSTEM LOAD REQUIREMENTS

1. Introduction

The specification defined three levels of electrical power requirements to be considered for each of the three engine classifications.

The generator power level definitions and their impact on the airframe (customer) and propulsion system are given in Table 1.

During the study it became apparent that the Power Level III generator/engine integration became impractical for the F404 and F103 because the generator would require more room than was available in the engine envelope.

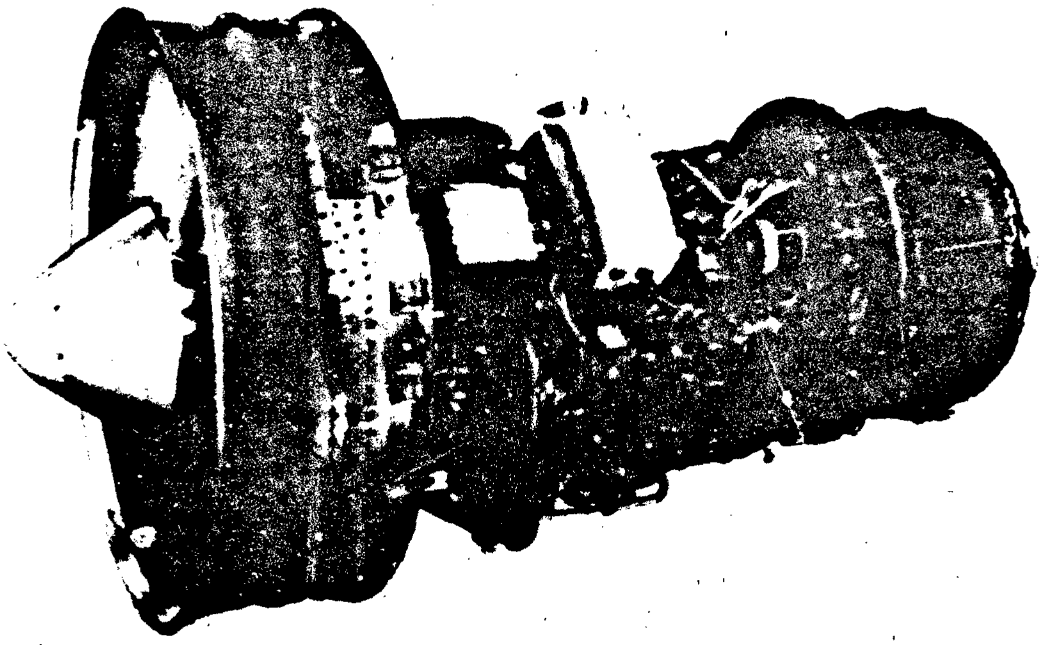
To provide a more comprehensive study, General Electric decided to add a fourth power level which was termed Level IIIA. This level, which is defined in Table 1, fills the significant gap between Level II and Level III.

System KVA ratings for each power level and each engine classification were established for consideration in the parametric design studies. These ratings were arrived at from (1) the ratings suggested in the specification, and (2) calculations which defined the starting and electrical load requirements of the application. The range of KVA ratings for each power level and each engine classification was then narrowed to a single rating for each combination for use in the engine alternator layouts and ultimately for use in the selection of an IEG/S concept for further study.

Table 2 shows the selected KVA ratings for each power level and each engine classification. Paragraphs which follow discuss how these selected KVA ratings were established.

2. Level I KVA Rating

A range of electrical system KVA ratings was given in the specification for Level I power for each of the three engine classifications. Generally, the range overlapped with the KVA rating requirements that were calculated for Level II power. It was necessary to select a single rating for each power level and each engine in order to limit the magnitude of engine design layout work being undertaken. The selection process for Level I involved a review of typical applications for each of the selected engines and knowledge of the KVA rating



SPECIFICATIONS

Weight, lb	1460
Length, in.	100
Maximum Diameter, in.	52
Fan-(Booster)/Compressor Stages	1/14
HPT/LPT Stages	2/4
Bypass Ratio	6.2
Pressure Ratio	22
Airflow, lb/sec	333
Maximum Thrust SLS, lb	9275
SFC	0.363
Thrust/Weight	6.4

Figure 3. TF34 Engine.

TABLE 1

GENERATOR POWER LEVEL DEFINITION

	AIRFRAME	PROPULSION
LEVEL I	KVA rating of IEG for typical state-of-the-art A/C or RPV requirement.	Mechanically driven accessories.
LEVEL II	Same as Level I, except must size IEG/S for electric starting.	Same as Level I.
LEVEL III	Provide all secondary power to airframe and propulsion in the form of electrical power. (Customer air bleed replaced by equivalent electric power.)	Eliminate mechanical engine accessory drive system. All engine secondary power systems driven electrically
LEVEL IIIA	Provide all secondary power to airframe and propulsion in the form of electrical power, except that bleed air is provided by conventional methods.	Eliminate mechanical engine accessory drive system. All engine secondary power systems driven electrically.

TABLE 2

SELECTED KVA RATINGS

Engine	I	II	IIIA	III
TF34 (RPV)	30/40	60	60/75	120
F404 Low Bypass	60/75	90	200	800
F103 High Bypass	75/90	120	300	1200

that had been established by calculation to meet the Level II requirements. Knowledge of the Level II requirement influenced the Level I selection in order to achieve a spread of KVA ratings being considered for each engine.

Table 3 shows the KVA ratings of electrical generating systems in specific applications of each of the selected engines. In Table 3 and elsewhere, a slash rating such as 30/40 KVA means, for example, that the continuous duty rating is 40 KVA but overload rating is based upon 30 KVA nominal. The standard overload requirement is 1.5 times the nominal rating for 5 minutes and 2 times the nominal rating for 5 seconds. The 30/40-rated system therefore has the following ratings:

Continuous	40 KVA	
5 Minutes	45 KVA	(1.5 x 30)
5 Seconds	60 KVA	(2.0 x 30)

Table 4 shows the KVA rating selected for each engine in the Level I application. The 30/40 KVA selection for the TF34 is in the middle of the ratings of actual applications shown in Table 3 and is lower than the Level II rating of 60 KVA. The rating selected for the F404 is higher than the application shown, because more power is required in typical applications and the Level II rating is 90 KVA. A straight 60 KVA rating also is a close contender for selection. The selected 75/90 ratings for the F103 fell within the range of actual applications and below the calculated Level II requirements of 120 KVA.

3. Level II KVA Rating

The KVA rating for each engine at Level II was established by calculating the system rating that would be required for starting the engine. The starter torque and power requirements are typically greater than Power Level I electrical power generation requirements.

a. Determination of Starter Rating

During a normal engine start, the starter drives the engine through the firing speed to self-sustaining speed and then assists the engine to accelerate until starter cutoff occurs.

Figures 4 through 6 show the engine drag torque curve vs. speed during start for different temperature conditions. The time to accelerate the engine rotor mass (inertia) is determined by the excess torque available from the starter. (The excess is the difference between starter torque and engine drag torque.)

TABLE 3

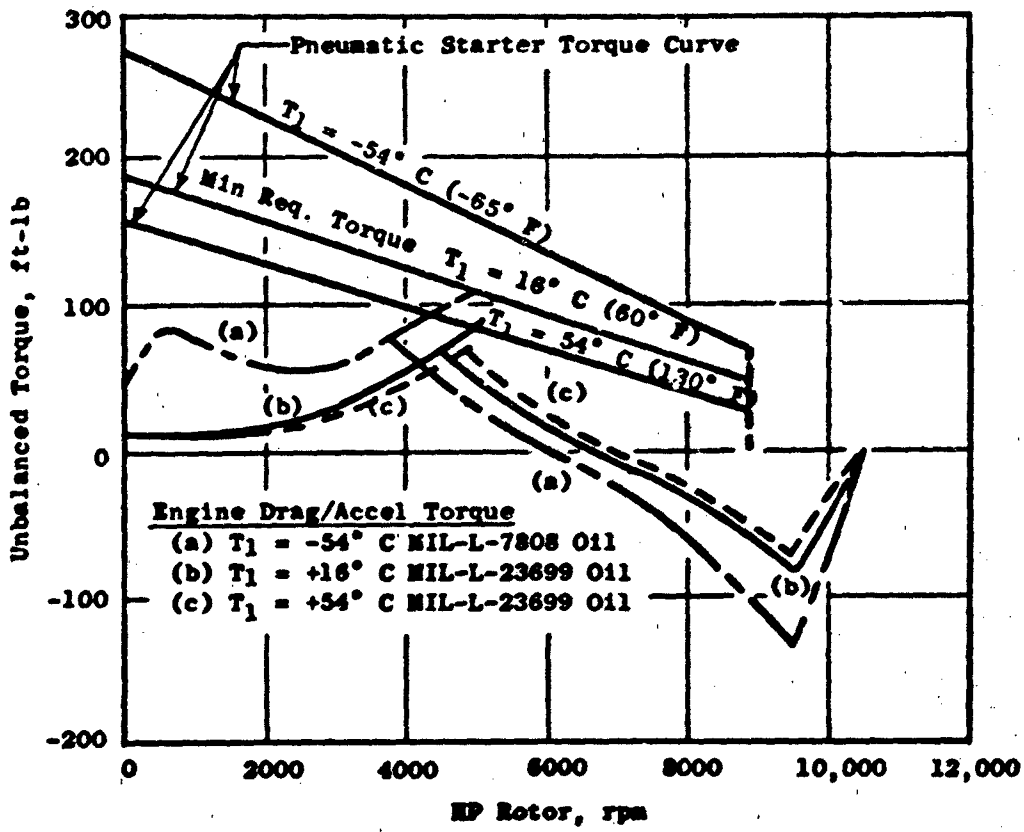
LEVEL I KVA RATINGS FOR SPECIFIC APPLICATIONS

	Aircraft/Service/Manufacturer	KVA Rating Per Engine	Number of Engines per Aircraft
TF34 (RPV)	A-10/USAF/Fairchild	30/40	2
	S-3A/USN/Lockheed	60/75	2
F404 (Low Bypass)	RPV from Specification	5 to 15	Typically 1
	F-18/USN/McDonnell-Douglas	30/40	2
	747/Commercial/Boeing	60	4
F103 (High Bypass)	DC-10/Commercial/McDonnell-Douglas	90	3
	ATCA (Tanker, DC-10)/USAF/ McDonnell-Douglas	90	3
	E-4A/B (Command Post 747)/ USAF/McDonnell-Douglas	2 x 150	4
	A-300B/Commercial/Airbus Industrie	90	2

TABLE 4

LEVEL I KVA RATING SELECTION

TF34 (RPV)	30/40
F404 (Low Bypass)	60/75
F103 (High Bypass)	75/90



Notes:

(a) No customer air bleed during starting.

(b) Power extraction permitted between starter cutout and ground idle, up to 34 hp providing starter cutout is at least 8000 rpm ±200 rpm.

(c) Minimum engine firing speed 3900 rpm.

Figure 4. Starting Torque and Speed Requirements (Sea Level Static Condition) - F404 Engine.

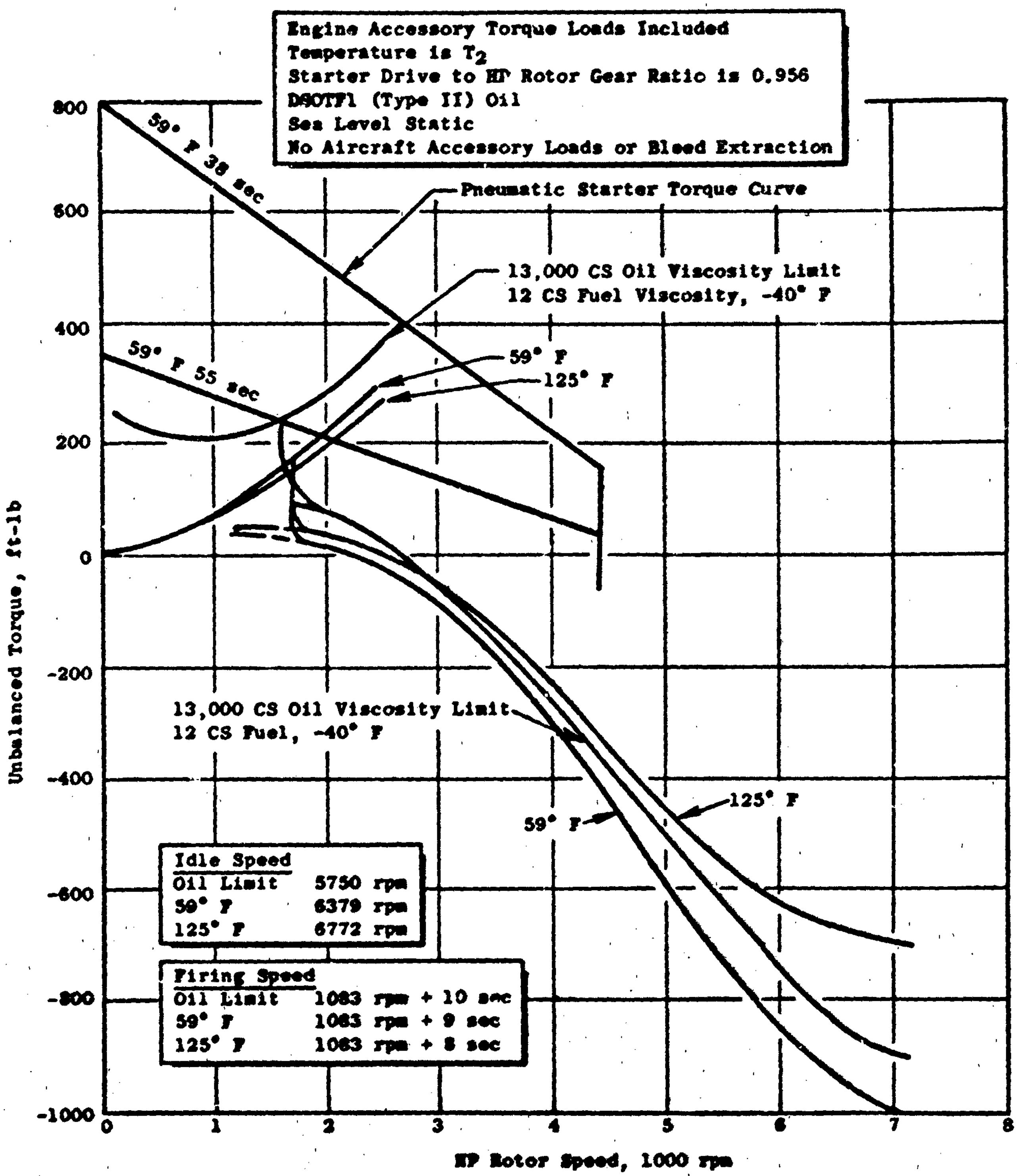


Figure 5. Starting Torque and Speed Requirements (Sea Level Static Condition) - F103 (CF6-50) Engine.

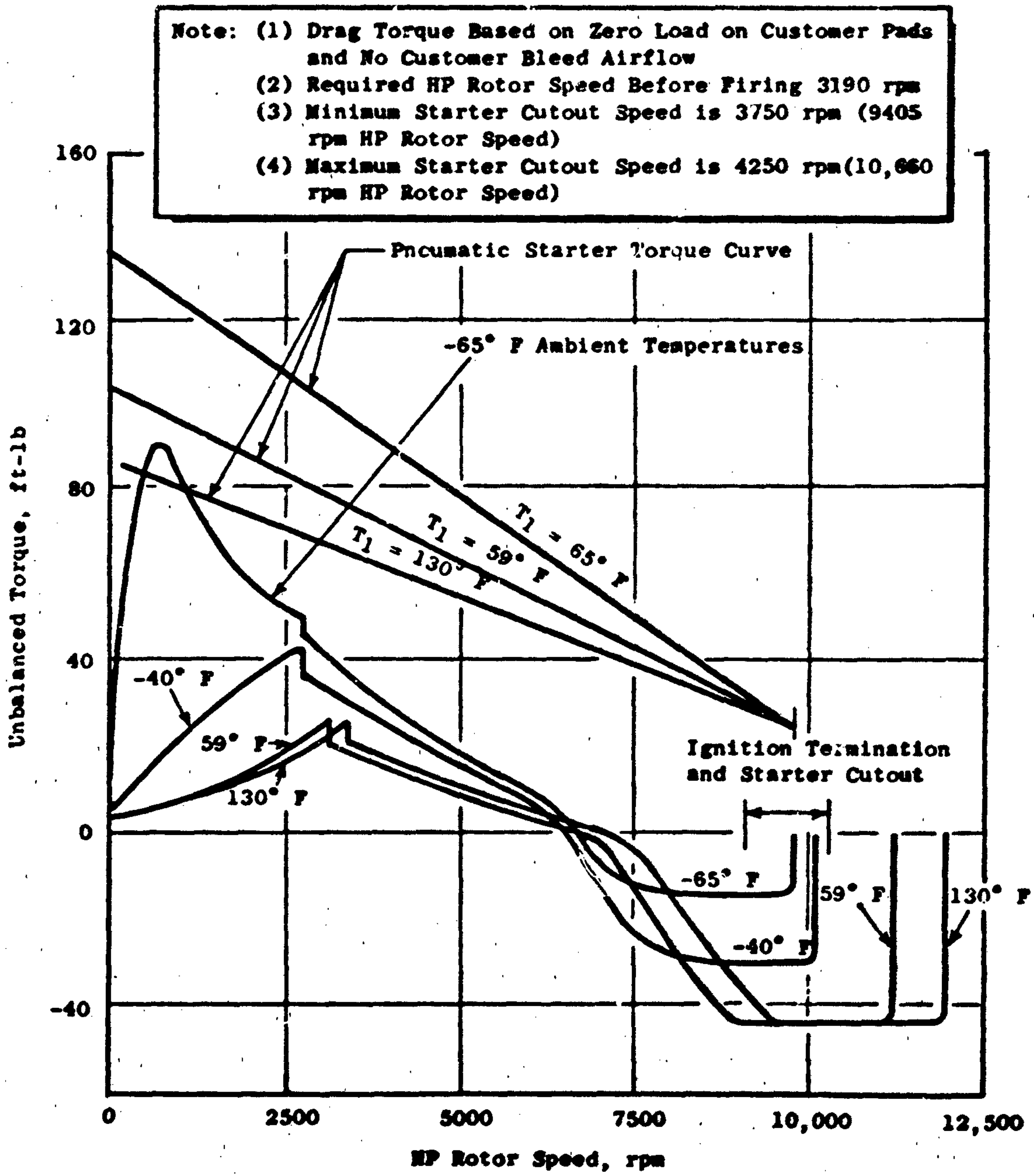


Figure 6. Starting Torque and Speed Requirements (Sea Level Static Condition) - TF34 Engine.

The resultant time to reach engine idle is derived from the following basic equation:

$$T_N = I_T \alpha \text{ [lb-ft]} \tag{1}$$

$$\text{with } \alpha = \frac{d\omega}{dt} \text{ angular acceleration [rad/sec}^2\text{]} \tag{2}$$

Then from (1) follows:

$$T_N = I_T \frac{d\omega}{dt} \text{ and the time to idle} \tag{3}$$

$$t = I_T \int_0^{\omega_{idle}} \frac{d\omega}{T_N} \quad \text{[sec]} \tag{4}$$

$$T_N = T_S \pm (T_E + T_A) \tag{5}$$

The actual calculation of start time using equation (4) is performed by taking $\Delta\omega$ (Δrpm) increments, using the average torque for each increment and determining Δt for each increment.

$$\Delta t = \frac{I_T \times 2\pi/60 \times (\Delta \text{rpm})}{T_N} \quad \text{[sec]} \tag{6}$$

$$\text{and } t = \Sigma \Delta t \quad \text{[sec]} \tag{7}$$

T_N = Net accelerating torque [lb-ft]

T_S = Steady-state starter torque [lb-ft]

T_E = $\pm$ engine torque [lb-ft]

T_A = Accessory torque reflected to the HP-rotor [lb-ft]

I_T = Total inertia of the engine HP rotor system [slug ft^2] + accessories referenced to the high pressure (HP) rotor

ω = Angular velocity (1/sec)

t = Time (sec)

The determination of electric starter torque and torque versus speed characteristic was established on the basis of equal start-time performance achieved by presently used pneumatic starters.

Table 5 lists engine start data, including start-time requirements for pneumatic starts.

General Electric uses a computer program which calculates the engine speed versus time for a given VSCF starter system rating and a given engine. The drag torque data, minimum idle speed, and inertia of the engine, torque characteristics of the IEG/S system, are inputs for the computer program. A function generator representing the starter system and another one representing the engine are set up in the computer. The difference between starter torque and engine drag torque establishes the net torque for acceleration. This net accelerating torque is integrated and divided by inertia to get speed, which is the input to the function generators. Using this program, calculations of time-to-idle speed for IEG/S systems of various KVA ratings were made for each engine. Plots of these data are given in Figures 7, 8, and 9 for the TF34, F404, and F103 engines respectively.

For the TF34, a six-phase machine in a 50 KVA rated system would get the engine to idle speed in the 30-second maximum start time. A 60 KVA system is recommended, however, since it is a more common rating. A nine-phase machine in an 80 KVA system would drive the F404 engine to idle speed in about 37 seconds, but a 90 KVA system is recommended as a more standard rating. For the F103 engine, a nine-phase machine in a 120 KVA system is recommended.

Table 6 shows the result of the analysis for the required starter/generator KVA ratings and predicted start times.

Figure 10 shows the expected speed/torque curve for a 60 KVA, permanent magnet, IEG/S system. The system is used at its 1.5 p.u. (per unit) rating for start, so these data actually reflect a system operating with 90 KVA of input.

As stated above, the curves of Figure 10 are for 90 KVA of input or a 60 KVA system operating at 1.5 p.u. This rating corresponds to the Level II system for the TF34 engine. For a 90 KVA system, which is recommended for the F404 engine, the same curves apply but the ordinate-axis power values are multiplied by 1.5. The 85 ft-lb. low frequency starting torque, for instance, would become 127.5 ft-lb. The 120 KVA system recommended for the F103 would also utilize the same curves but with the ordinate axis values multiplied by 2.

For Power Levels IIIA and III, the input volt-amps to the starting machine would be limited to 90 KVA, 135 KVA, and 180 KVA, respectively, for the TF34, F404, and F103 engines, since this input level is sufficient to accomplish engine starting.

TABLE 5
ENGINE GROUND START DATA

		F404	F103	TF34
Ignition speed core rpm (std. day S.L.)	Start	1680	1000	3188
	Termination	7560	6340	10,000
Ground idle speed core rpm	Std day S.L.	10,500	6340	11,300
	Min idle (-)65°F S.L.	8715	5262	9380
100% operating speed core rpm (std. day S.L.)		16,810	9827	17,600
Generator overspeed (122% core speed) rpm		20,508	11,990	21,472
Max start time to idle (air start) (std. day S.L.), sec.		35-40	Typically 35-38	30
Mass moment of inertia HP-rotor, lb-ft^2		80.5	660	40.3
Direction of rotation of HP-rotor looking forward		Clockwise	Clockwise	Clockwise

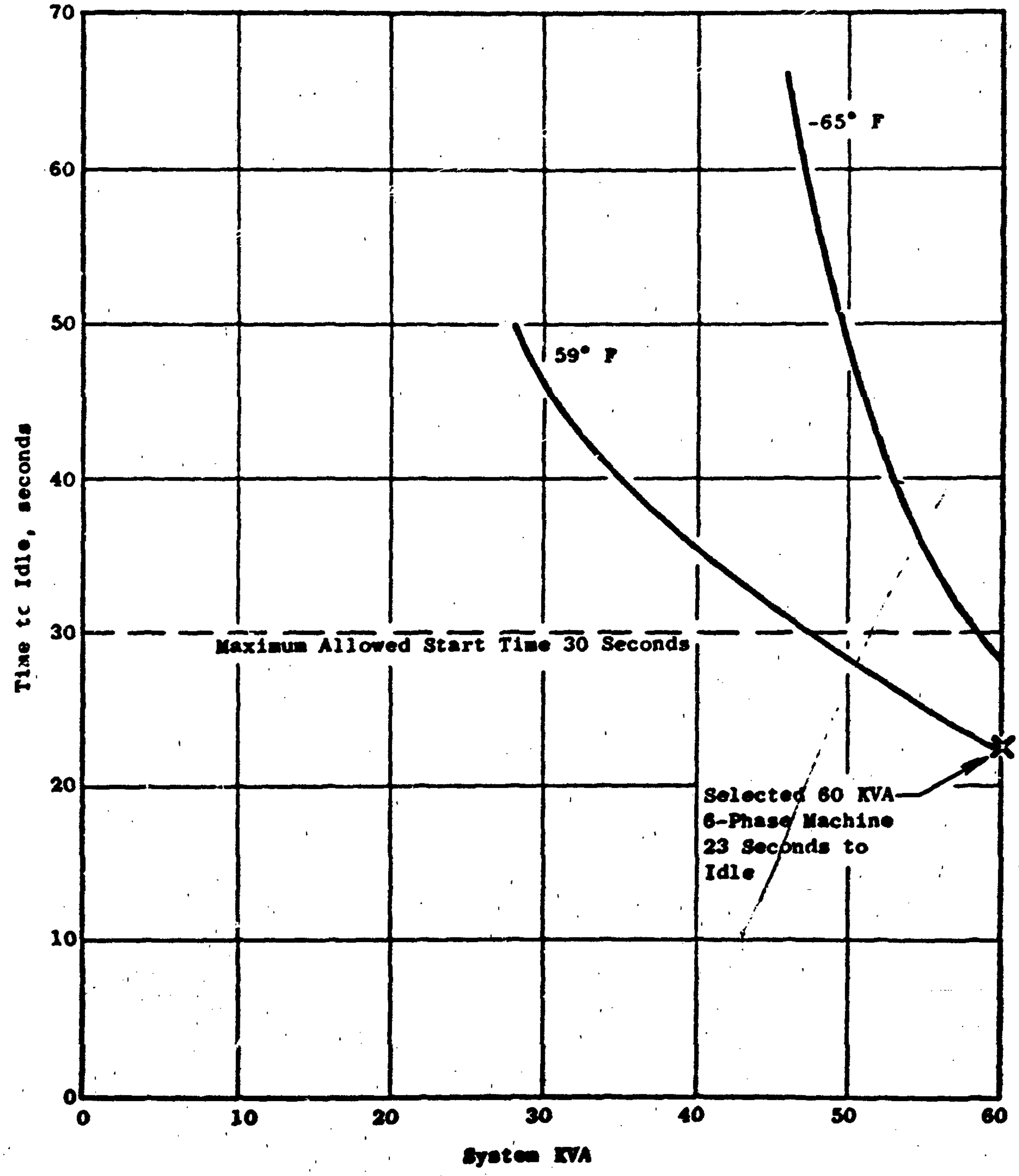


Figure 7. TF34 Start Time.

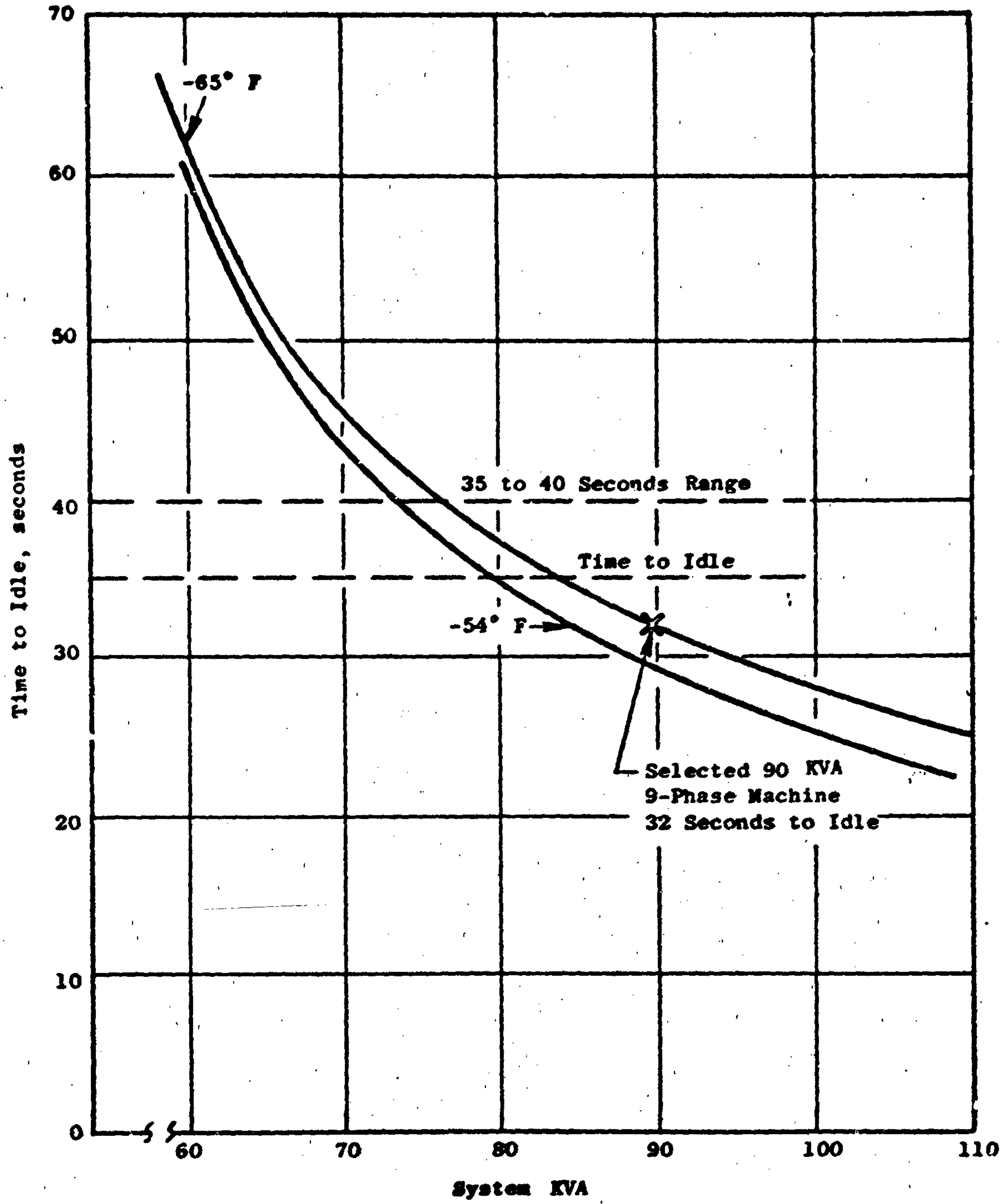


Figure 8. F404 Start Time.

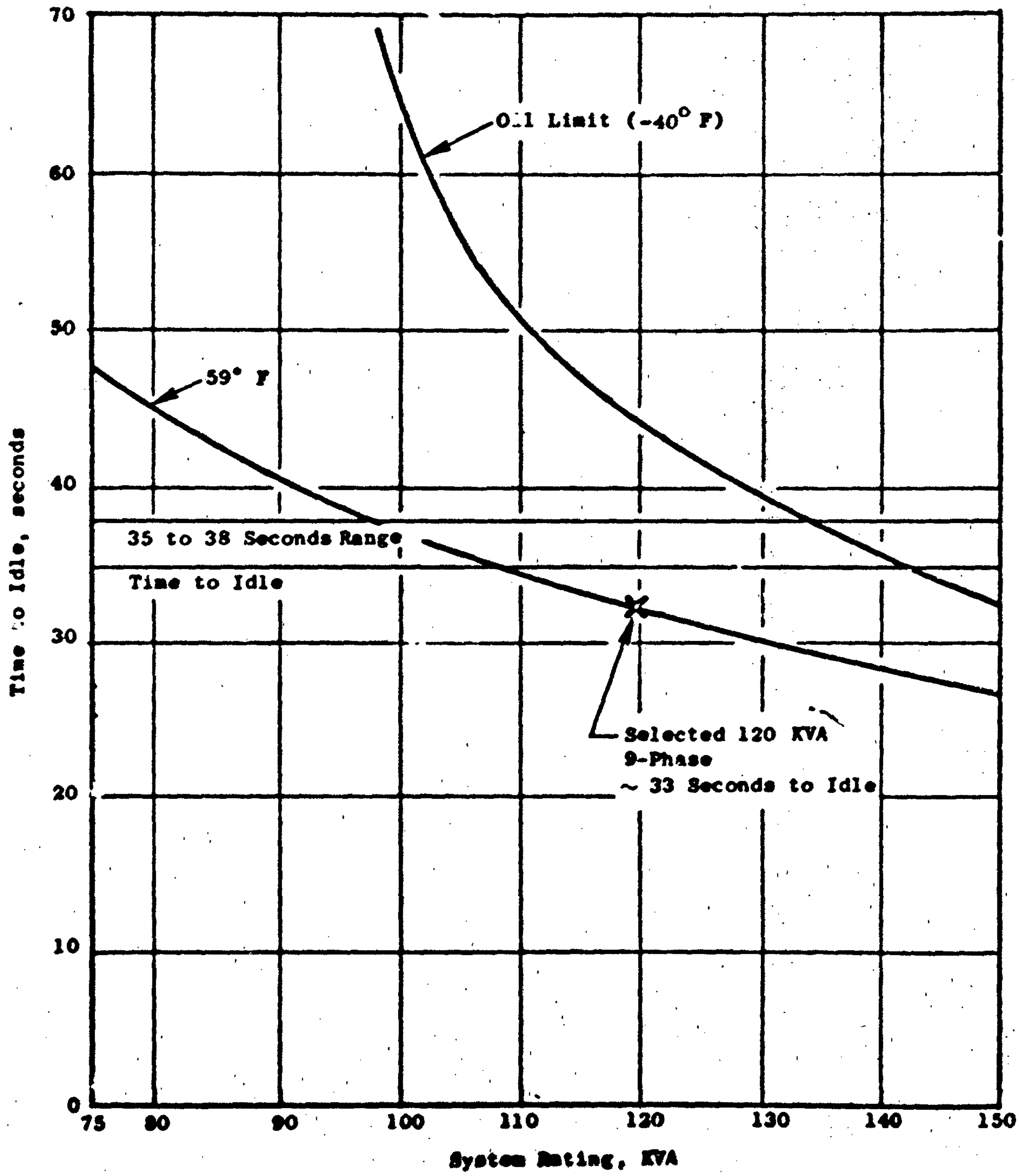


Figure 9. F103 Start Time.

TABLE 6

POWER LEVEL II KVA RATING

Engine	Electrical System KVA Rating	Machine Phases	Expected Time to Idle, S.L. Standard Day (Seconds)*
TF34	60	6	23
F103	120	9	33
F404	90	9	32

*Based on 1.5 x electric system KVA rating for starting and cross-starting without customer accessory load extraction.

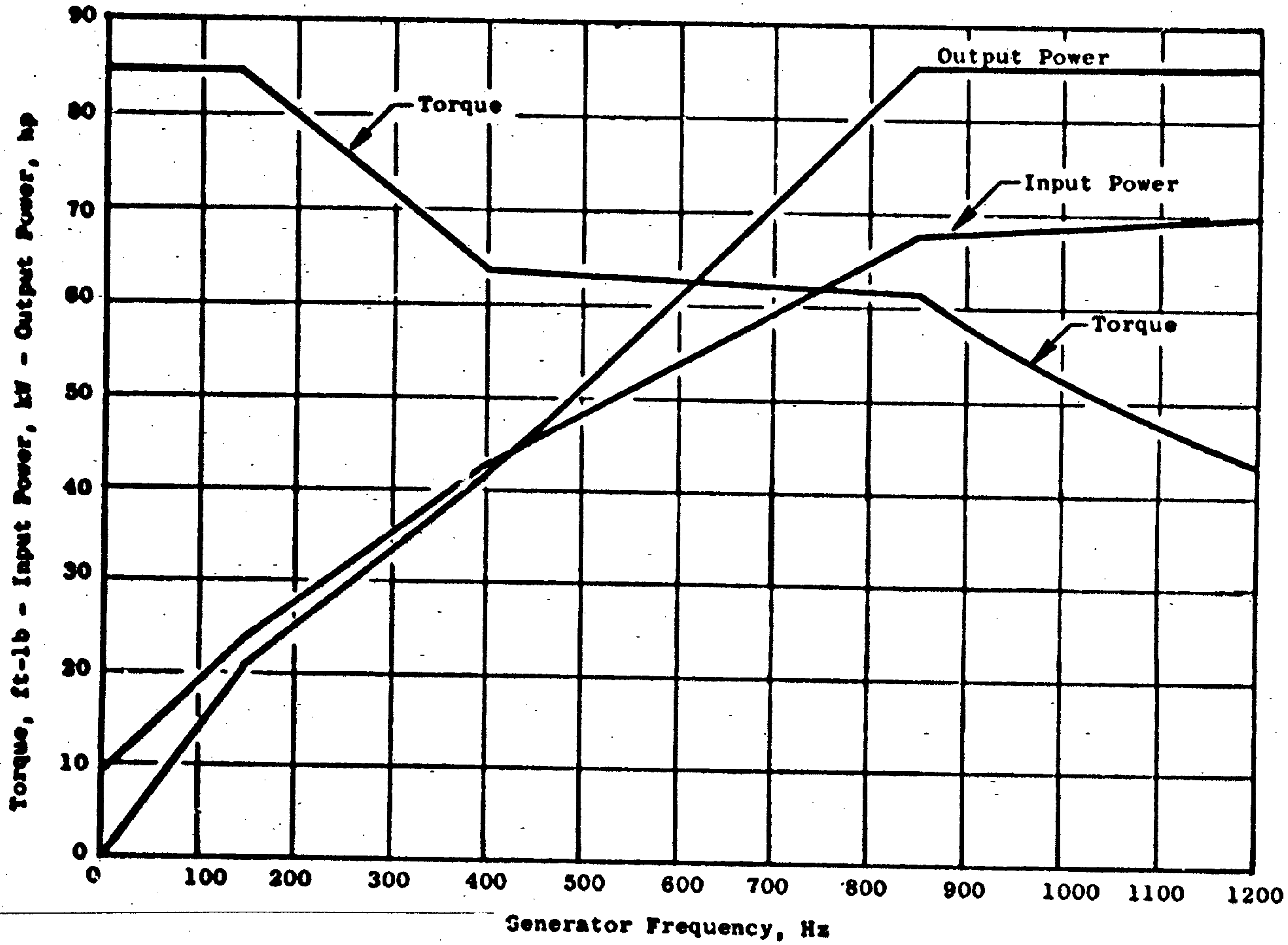


Figure 10. Torque and Power versus Speed (Machine Frequency) for 60 KVA PM Generator/Starter (VSCF) Electrical System.

b. Power Extraction During Cross Start

In a cross-start situation, one IEG/S system is in the generate mode supplying electrical power to a second IEG/S system which is in the start mode. The electrical power supplied is normally limited by the start rating for each system. The power quality from any but the stiffest power source is degraded by the start system load during the first 10 to 15 seconds of the start period. The degradation occurs because the power factor of the load is very low until the starting machine gains a few hundred rpm. For cross-start, the generating VSCF system selected for each of the three engines can supply an additional electrical load of up to 20 percent of its continuous duty rating. This additional power extraction during the cross-start for each engine is shown in Table 7.

The power will have a 10 to 12 percent harmonic distortion and low line-to-neutral voltages (108 VRMS) during the first 10 to 15 seconds of the start period. As the engine being started comes up to idle speed the power quality will return to normal.

The above circumstances apply whether on the ground or in flight, though the in-flight starting interval is expected to be shorter since the start will be assisted by windmilling of the engine. It should be noted that the heavy, low power-factor load during the first half of the start mode may also cause distortion of the power from a ground power source when starting the first engine. It will be necessary to include this loading consideration when selecting or specifying an external power source that will be used to supply engine start electrical power.

For Levels IIIA and III, there is excess capacity in the VSCF generator/starter beyond what is used by the system in the start mode. The excess electrical generation power will be supplying other electrical system loads. Since the start system with the initial low power factor will represent less of the total electrical systems load, the power quality will not be degraded significantly.

4. Power Level III

Power Level III is defined as an all-electrical secondary power extraction, including customer bleed air.

TABLE 7

ADDITIONAL POWER EXTRACTION DURING CROSS START

Engine	Level II System Rating (KVA)	Additional (Accessory) Power Extraction (KVA)
TF34	60	12
F404	90	18
F103	120	24

This IEG/S combines the function of electric power generation and engine starting. The all-electric secondary power extraction has its highest potential in the application of the all-electric aircraft. Limitation of the physical size of the generator, however, determines the applicability of the design.

a. Determination of Generator/Starter Rating

Power for the operation of aircraft and engine accessories is extracted conventionally from the engine in the form of: (1) bleed air for the aircraft environmental control, and (2) power (mechanical) to drive hydraulic pumps, electric generators, and engine accessories such as fuel pump, lube pump, and the control alternator.

Typical customer bleed air requirements have been established from field experience for commercial aircraft. The maximum allowable customer bleed air limits are controlled by the engine model specification. Bleed air quantities and their respective equivalent horsepower are listed in Table 8.

The equivalent hp is derived from the adiabatic compression process of air by the following relationship:

$$P_c = \frac{\dot{m} \; c_p \; T_c}{0.707} \quad \text{Power of compression [hp]} \tag{8}$$

$$\dot{m} = \text{Bleed air flow} \left[\frac{\text{lb}}{\text{sec}}\right] \tag{9}$$

$$c_p = 0.24 \; \frac{\text{BTU}}{\text{lb}^{\circ}} \quad \text{specific heat for air} \tag{10}$$

$$\Delta T_c = (T_{Bleed_C} - T_{Inlet_C}) \quad [(^{\circ}R)]$$

T_{cBleed}, $T_{c\,Inlet}$ actual compressor air temperature [°R]

Customer bleed air is used for the following typical applications:

1) Cabin pressurization
2) Cabin environmental control
3) De-icing of engine nacelle
4) Wing leading edge anti-icing
5) Cargo bay heating
6) Electronic equipment cooling
7) External windshield de-ice, internal windshield defog

TABLE 8
CUSTOMER BLEED AIR SECONDARY POWER REQUIREMENTS

Engine	TF34-100/A-10				F103 (CF6-50)				F404/F-18				
Operating Mode	Ground Idle	Takeoff	Cruise 90% 25K ft 0.47M	Descent 15K ft 0.60M	Ground Idle	Takeoff	Cruise 33K ft 0.82M	Descent 36K ft 0.81M	Ground Idle	Takeoff	Cruise 64% 43K ft 0.83M	Flight Idle	Loiter 0.3M 14.00 FT HR
Engine Bleed Air Limit, % of Core Flow	4%				5%				5%				
Core Flow (Physical), lb/sec	8.97	47.0	19.2	7.29	38.4	273	121	78	25.5	106.4	24.7	25.5	42.7
Max Bleed Air Flow, lb/sec	0.36	1.88	0.768	0.29	1.92	13.65	6.05	3.90	1.27	5.32	1.23	1.27	2.13
Typical Customer Bleed Air Flow %	1.0	0.40	1.0	1.0	5.0	1.0	2.1	3.6	1.5%				
lb/sec	0.10	0.17	0.038	0.057	1.92	2.8	2.6	2.8	0.38	1.59	0.37	0.38	0.64
Compressor Stage (Bleed)		10			8				7				
T_2 °R	519	519	473	499	519	519	455	466	519	519	443.9	519	528.7
P_2 psia	14.7	14.7	6.35	10.58	14.7	14.7	5.91	6.7	14.7	14.7	3.70	14.70	15.64
T_{Bleed} °R	720	1160	1029	701	675	1284	1099	920	852	1431	1150	852	1015
P_{Bleed} psia	31	158	61.8	22.8	222	190	77.7	46.4	60.6	353.5	70.9	60.6	111.9
Max Equivalent hp	24.5	409	145	20	102	3546	1323	601	144	1648	295	144	352
Typical Equivalent hp (Normal Operation)	5.87	37.0	7.2	3.9	102	728	570	432	43.0	492.0	88.7	43.0	105.7

The all-electric secondary power extraction will require the conversion of electric energy to pressurized air, suitable to perform the tasks of cabin pressurization and air-conditioning.

Table 9 shows the data that were assembled to define secondary power requirements other than bleed air for each engine in a specific aircraft application. The data show the secondary power per engine and the bottom line is the power in kilowatts per engine for all secondary power less the electrical system and less bleed air. Table 8 tabulates the horsepower per engine for customer bleed air in each of the same applications as Table 9.

The results from Table 8 are indicative of the large power requirements necessary to replace engine customer bleed air with electric power. Although only a fraction of the engine bleed limit was used to establish equivalent electric power demands, emergency situations (one engine out) allow bleed air supply up to the specification limit per engine. However, the degree to which electric power is required to replace engine bleed air on military aircraft remains to be determined and should be investigated with an airframe manufacturer's support. Table 10 summarizes the total power requirements for Level III for maximum and normal conditions. Engineering judgement was used to establish the actual KVA rating for the all-electric power generation system. Peak power requirement and duration, as in the case of the one-engine-out condition, are two of those considerations.

The selected Power Level III system KVA ratings are listed in Table 11.

Power Level III KVA ratings for the F404 and F103 engines are too large for generator/engine integration. Therefore, a fourth power level (IIIA) was introduced to complement the study. It is defined in the following paragraph.

b. Power Level IIIA

Power Level IIIA is the same as Power Level III except that customer bleed air is provided by the engine compressor.

The required Power Level IIIA KVA system rating can be directly extracted from Table 10. The selected Power Level IIIA system KVA ratings are shown in Table 12.

TABLE 9
SECONDARY POWER REQUIREMENTS OTHER THAN BLEED AIR

Engine	TF34-100 (A-10)				F103 (CF6-50) (747)				F404 (F-18)				
Operating Mode	Ground Idle S.L Std. Day	Takeoff S.L Std. Day	Cruise 25K ft, 0.47M	Descent 25K ft, 0.39M	Ground Idle S.L. Std. Day	Takeoff S.L. Std. Day	Cruise 33K ft, 0.82M	Descent	Ground Idle S.L. Std. Day	Takeoff S.L. Std. Day	Cruise	Descent	Takeoff After-burner
Main Fuel Pump and Main Engine Control (hp)	3.5	15.5	9.75	4.6	25.6	94	44.1	47.6	5.0	60	55	30	60 to 70
Afterburner Pump (hp)	--	--	--	--	--	--	--	--	--	--	--	--	35 to 75
Main Lube and Scavenge Pump (hp)	1.1	2.5	2.3	2.1	1.10	20.0	2.5	5.0	0.20	0.90	0.80	0.5	0.90
CSD (VSCF) Generator (hp)	30	36	34	32	69.5	141.4	138.9	141.4	52 -- 53	70 136 115	70 136 115	67 133 112	70 Normal 136 Combat 115 Single Eng.
(OEI)		(56)	(54)	(52)									
Total, Hydraulic Pumps (hp) (# of pumps/eng.)	19	19	19	19	25.6 (2)	131.2 (2)	51.2 (2)	147.8 (2)					
Variable Exhaust Nozzle P/C	--	--	--	--	--	--	--	--	0	3.4	3.3	3.2	3.6 to 20
Total (hp) Extraction, Present System	53.6	73.0	65.0	57.7	121.8	386.6	236.7	341.8	57.2 to 58.2	134.3 to 200.3	129.1 to 195.1	100.7 to 166.7	169.5 to 301.5
hp Extraction, Normal Operation (Minus CSD)	23.6	37	31	25.7	52.3	245.2	97.8	200.4	18.2	139.3	134	106	244.5
Electric Power Gen. (Minus CSD), $\eta = 0.80$: N_{kW} = hp x 0.746/η	22.0	34.5	29.0	24.0	48.8	228.7	91.2	187.0	17.0	130.0	125.0	99.0	228.0

TABLE 10

TOTAL LEVEL III POWER REQUIREMENT FOR MAXIMUM AND TYPICAL CONDITIONS

Item	Engine		TF34-100				F103 (CF6-50)				F404				
	Operating Mode		Ground Idle	Take-Off	Cruise	Descent	Ground Idle	Take-Off	Cruise	Descent	Ground Idle	Take-Off	Cruise	Descent	Take-Off After-Burner
1	High Pressure Turbine rpm, Minimum		9380	95% 16,720	84% 14,784	70% 12,320	5262	95% 9335	84% 8254	70% 6880	8715	95% 15,970	84% 14,120	70% 11,770	15,970
2	Electrical Power to Drive Accessories, Except CSD (VSCF), kW		22.0	34.5	29.0	24.0	48.8	228.7	91.2	187.0	17.0	130.0	125.0	99.0	228.0
3	Level I Customer Electric Power (CSD), kW		40	40	40	40	90	90	90	90	75	75	75	75	75
4	Power Generation 2+3 Without Considering Bleed Air, kW		62	74.5	69	64	138.8	318.7	181.2	277	92	205	200	174	303
5	Equivalent Bleed Air Power, kW	Max	18.3	305	108	15	76.1	2646	987	448	107	1230	220	107	263
		Typ.	5.1	27.6	5.4	3.0	76.1	543.3	425.4	322.4	32	367	66.2	32	79
6	Total Power 4+5, kW	Max	80.3	379.5	177	79	215	2965	1168	725	199	1435	420	281	566
		Typ.	67.1	102.1	74.4	67	215	862	606.6	599.4	124	572	266.2	206	382

TABLE 11

LEVEL III KVA RATING SELECTION

Engine	KVA
TF34	120
F103	1200
F404	800

TABLE 12

LEVEL IIIA KVA RATING SELECTION

Engine	KVA
TF34	60/75
F103	300
F404	200

SECTION V

PARAMETRIC DESIGN STUDIES OF INTEGRATED ENGINE STARTER/GENERATORS

The objective of this study has been to investigate the feasibility of integrating permanent magnet excited synchronous machines into jet engines to serve as electrical power generators as well as engine starter motors. Tradeoff design studies for the electrical machines have been performed to establish the optimum machine geometries and dimensions for these applications. Section A will describe the boundary conditions for the design studies. Sections B and C will discuss the two design concepts studied for this project. Finally, Section D will present general conclusions.

A. BASIC CONSIDERATIONS AND GUIDELINES

Designing an electrical machine for an aircraft engine integration application differs from typical electrical machine design primarily because of the additional restrictions imposed by the engine environment. The basic design boundary conditions fall into three groups:

1. Design constraints determined by the engine environment.
2. Design constraints set by the electrical system.
3. Machine inherent design constraints.

1. Design Constraints Determined by the Engine Environment

Integration of the electrical machine into an engine implies the mounting of the electrical machine onto one of the engine shafts in a suitable location. If the engine is to be started by the electrical machine, the electrical machine should be mounted on the high-speed compressor shaft. Since this shaft generally has a higher base speed and a smaller speed range than the fan shaft, the coupling of the electrical machine to it is more advantageous from a machine design point of view. Table 13 provides engine interface data on the compressor shaft for each of the three engines. A high base speed results in a smaller machine for a given power level, and a smaller speed range reduces the mechanical design problems.

all generators/starters are mounted on the high-speed-compressor shaft. ocation requires that a generator be designed with a shaft diameter larger is generally chosen. This limits the possible geometry variations, especially

TABLE 13

DESIGN REQUIREMENTS ENGINE INTERFACE DATA

(Engine Ground Start Data)

Engine		F404	F103 (CF6)	TF34
Ignition Speed Core - rpm (St'd Day S.L.)	Start	1680	1000	3188
	Termination	7560	6340	10,000
Ground Idle Speed Core - rpm	St'd Day S.L.	10,500	6340	11,300
	Min. idle (-) 65°F S.L.	8715	5262	9380
100% Operating Speed Core - rpm (St'd Day S.L.)		16,810	9827	17,600
Generator Overspeed (122% Core Speed), rpm		20,508	11,990	21,472
Max Start Time to Idle (St'd Day S.L.) (sec.)		35-40	Typical 35-38	30
Mass Moment of Inertia HP - rotor (lb - ft^2)		80.5	660	40.3
Direction of Rotation of HP - Rotor Looking Fwd.		CW	CW	CW
100% Operating Speed Fan rpm (St'd Day S.L.)			3432 (122% = 4187)	
Fan rpm at Ground Idle	St'd Day S.L.		800	
	-65°F S.L.		700	
Minimum Shaft Diameter (in.) (Depending Upon Selected Location)		2.95	9.50 7.50 6.80 8.20	4.20 3.70 3.10

in connection with the maximum rotor diameter limitations due to mechanical stresses. The overspeed requirements, which are larger than usual for high-speed machines (122 percent versus 110 to 115 percent of the maximum operating speed), add additional design constraints to the rotor design.

The generator location at the front end of the high pressure shaft of the engine provides a moderate temperature environment. The environmental condition will influence the design of the machine to some degree. However, the most severe effects of environmental temperature occur in the nonoperative mode of the engine during the time after shutdown when temperature soakback may result in unacceptable magnet temperatures. This effect may influence the IEG/S location selection within the engine.

Relative axial movement between engine shaft and frame due to thermal expansion has to be considered, along with shaft runout, when determining electrical machine air-gap length, clearances, and operating characteristics. The effects of this movement are more significant for the disk-type PM machine than for the cylindrical machine.

The safety and maintenance requirements for the engine influence the machine design significantly. The need for high MTBF and high reliability significantly reduces the current densities for the machine windings and demands a heavier insulation system. Additional safety devices such as rotor disconnects and/or electrical fuses are mandatory in this application to limit fault current effects in the PM machine.

2. Electrical System Design Constraints

The generator is connected to a solid-state power converter which fulfills two functions: (1) it converts the frequency from the variable generator frequency to the constant 400 Hz electrical systems frequency, and (2) it regulates the voltage to match the unregulated generator voltage to the constant system voltage. This power conditioning is achieved by a direct frequency converter which is a cycloconverter. The cycloconverter imposes its special interface operating conditions on the electrical machine design. Table 14 lists typical electrical operating conditions at the generator terminals as a function of the system loads for a nine-phase cycloconverter system. The magnetic design of the generator has to meet the voltage, current, and power factor conditions as listed for the fundamental, while the thermal design has to accommodate the slightly high RMS current values. Sample designs have established that the

TABLE 14
ELECTRICAL REQUIREMENTS AT INTERFACE

9 PHASES

SYSTEM P_s (KVA)	PF	U_{min} (V)	I_f' (A/KVA)	I_h' (A/KVA)	P_{mf}'	DF	Duty
0		155	0.31	0.33	0.47	0	Cont.
1.0	0.75	155	0.79	0.86	1.10	0.71	Cont.
1.0	0.95	155	0.92	0.99	1.28	0.77	Cont.
1.5	0.75	155	1.26	1.36	1.75	0.68	5 Min.
1.5	0.95	155	1.38	1.49	1.92	0.77	5 Min.
2.0	0.75	145	1.74	1.88	2.27	0.69	5 Sec.
2.0	0.95	145	1.86	2.01	2.43	0.80	5 Sec.

P_s = System Rating

PF = System Power Factor

U_{min} = Minimum Line-to-Neutral Generator Voltage

I_f = Fundamental Current Per Phase $\quad I_f = P_s \cdot I_f'$

I_h = Heating or RMS Current Per Phase $\quad I_h = P_s \cdot I_h'$

P_{mf} = Fundamental Power $\quad P_{mf} = P_{mf}' \cdot P_s$

DF = Generator Power or Displacement Factor

electromagnetic design point for the cylindrical air-gap PM machine should be the 2 per unit overload point with the lower machine displacement factor. The electromagnetic design point for the disk-type PM machine was found to be the 1.5 per unit overload point with the larger current value.

In addition, since the machine has to provide the commutation power for the cycloconverter, a maximum allowable commutation inductance is specified for the generator. Since both of the PM machine types considered will have no amortisseur windings, the basic machine design must have a low synchronous reactance level. The radial gap (cylindrical) PM machine could be equipped with a special damper winding, but this consideration has been discarded because of its degrading effect on rotor reliability.

PM generators lack any voltage control. The cycloconverter will provide for that function. However, the natural voltage regulator is to be kept low in order to stay within the peak voltage limits of the SCR's in the cycloconverter. Any cycloconverter operation requires certain relationships between input and output frequency, depending upon the flow of the commutating power. In order to reduce the filter requirements in the converter to an acceptable level the generator minimum frequency should be approximately three times the output frequency. A general low frequency limit for the generators has been set at 1100 Hz for a 400 Hz system output frequency. A limit of 1200 Hz or higher at the minimum generator operation speed is more desirable. This frequency requirement, together with the minimum high-pressure shaft idle speed of the engines considered, determines the minimum number of pole pairs for the electrical machines.

3. PM Machine Inherent Design Constraints

Permanent magnet machines derive their magnetization power from permanent magnets. The total magnetization power is fixed with the design and cannot be altered for the finished machine. This locked-in magnetization power requires that the machine designer predict the load operating conditions more accurately than would be necessary for conventional machines.

A PM machine rotor also requires that only nonmagnetic materials be used outside the magnetic flux path in the rotor. In particular this means that the shaft supporting the rotor should be made from nonmagnetic steel. The low-speed fan shaft rotating inside the high pressure shaft does not need to be nonmagnetic, provided that the distance between fan shaft and magnet is larger than the centerline distance between the magnets.

The structural and electromagnetic characteristics of the materials selected for an electrical machine design have a significant influence on the machine performance, size, and weight. In recognition of the low weight requirements for aircraft applications, high performance materials have been selected. The magnetic stator laminations are made from 6 mil Permendur sheets. Permendur features a saturation flux density of 140 KL/in.^{2} and allows stator tooth and stator yoke flux densities of 130 KL/in.^{2} at the design point. All radial gap machines designed for this study use the same maximum flux density levels.

The permanent magnets selected (rare earth magnets) are of the $SmCo_5$ type. The maximum energy level of these magnets is continually being upgraded due to improvements in processing techniques and composition. The study has considered the influence of the magnet energy, and appropriate results will be shown. Unless otherwise stated, however, the designs will be carried out for magnets with a 20 megagauss-oersted (MGOe) energy product which have been stabilized at 150° C. The material characteristics for this state have been defined as:

$$B_r = 55.1 \text{ KL/in.}^2 \qquad \text{(remanence flux density)}$$

$$\mu_r = 1.03 \qquad \text{(re-coil permeability)}$$

These values are used throughout the tradeoff studies. The rotor construction for both machine concepts relies on retaining rings which are shrunk onto the magnet structure to keep it in compression up to the required overspeed level. The materials selected for these retaining rings are to provide a stress capability of 155,000 psi. The stress analysis utilized for the shrink rings in the tradeoff studies is based upon the thin cylinder theory and the results are not very promising. In view of the fact that a detailed rotor stress analysis is a long, complicated process, the simplified analysis has been chosen. Experience with both methods on a previous high-speed rotor design has shown good correlation, with the simplified analysis predicting between seven and ten percent higher stresses.

B. RADIAL CLEARANCE (CYLINDRICAL) MACHINES

The construction of the radial clearance machine is in principle similar to the 150 KVA PMG-VSCF system developed under U.S. Air Force Contract No.

F33615-74-C-2037. Figure 11 shows the cross-section of the rotor construction. The design key is the bimetallic shrink ring, which keeps the magnet and pole piece in the rotor under compression through the design overspeed. This construction requires high interference fits and high stresses at zero speed and only slightly higher stress levels at the overspeed condition. This small stress variation reduces the cyclic fatigue effects dramatically. Originally the following tradeoff parameters were to be considered:

- magnetic energy level/volume
- machine diameter
- machine speed
- voltage regulation/reactance level
- frequency/poles

The results of the initial sensitivity studies are described and discussed below.

The speed range is fixed for any given engine application. To illustrate the mechanical design limitations, the influence of maximum machine rpm on the maximum magnet height (radial direction) and on the minimum allowable thickness of the containment ring has been established for a given maximum rotor radius of 4-1/2 inches, a maximum containment ring stress of 150,000 psi, and an overspeed ratio of 1.22. Figure 12 shows the results and illustrates that at higher maximum operating speeds the containment ring thickness increases very rapidly. The maximum magnet height in radial direction, which is proportional to the magnet volume, becomes less as more space is needed for the containment structure. In other words, a nine-inch-diameter rotor becomes impractical at maximum operating speeds above 17,000 rpm.

The above curves are the basis for the curves presented in Figure 13. This figure shows the total machine weight, the electromagnetics weight, and the magnet weight as functions of magnet energy level in percent of those weights achieved for a magnet energy level of 21 MGOe. These calculations were made for constant operating speed and power. The curves express that an increase in magnet energy basically reduces magnet weight and volume necessary for a given power and speed point. The reduction in electromagnetics weight is principally due to the reduction in magnet weight and thus rotor weight. The weight of the stator electromagnetics, for example, remains unchanged, as does the total weight. In other words, an increase in magnet energy has two effects on the machine design:

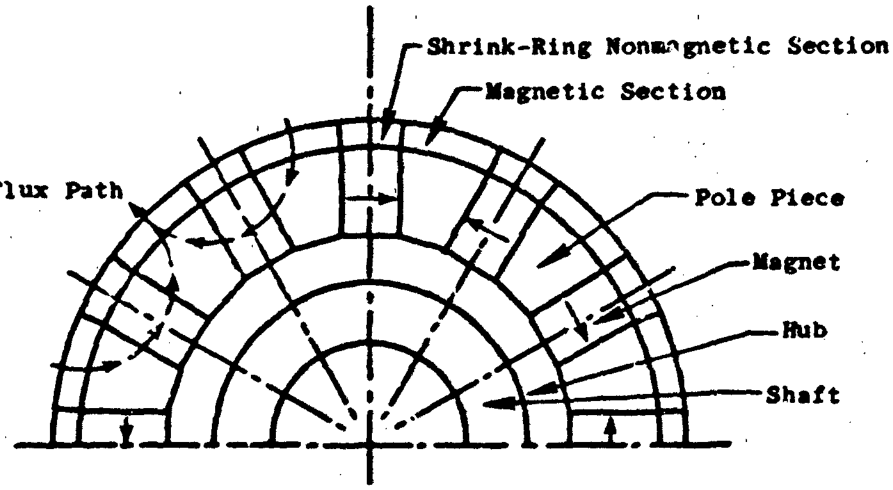


Figure 11. Rotor Construction for Cylindrical Machine.

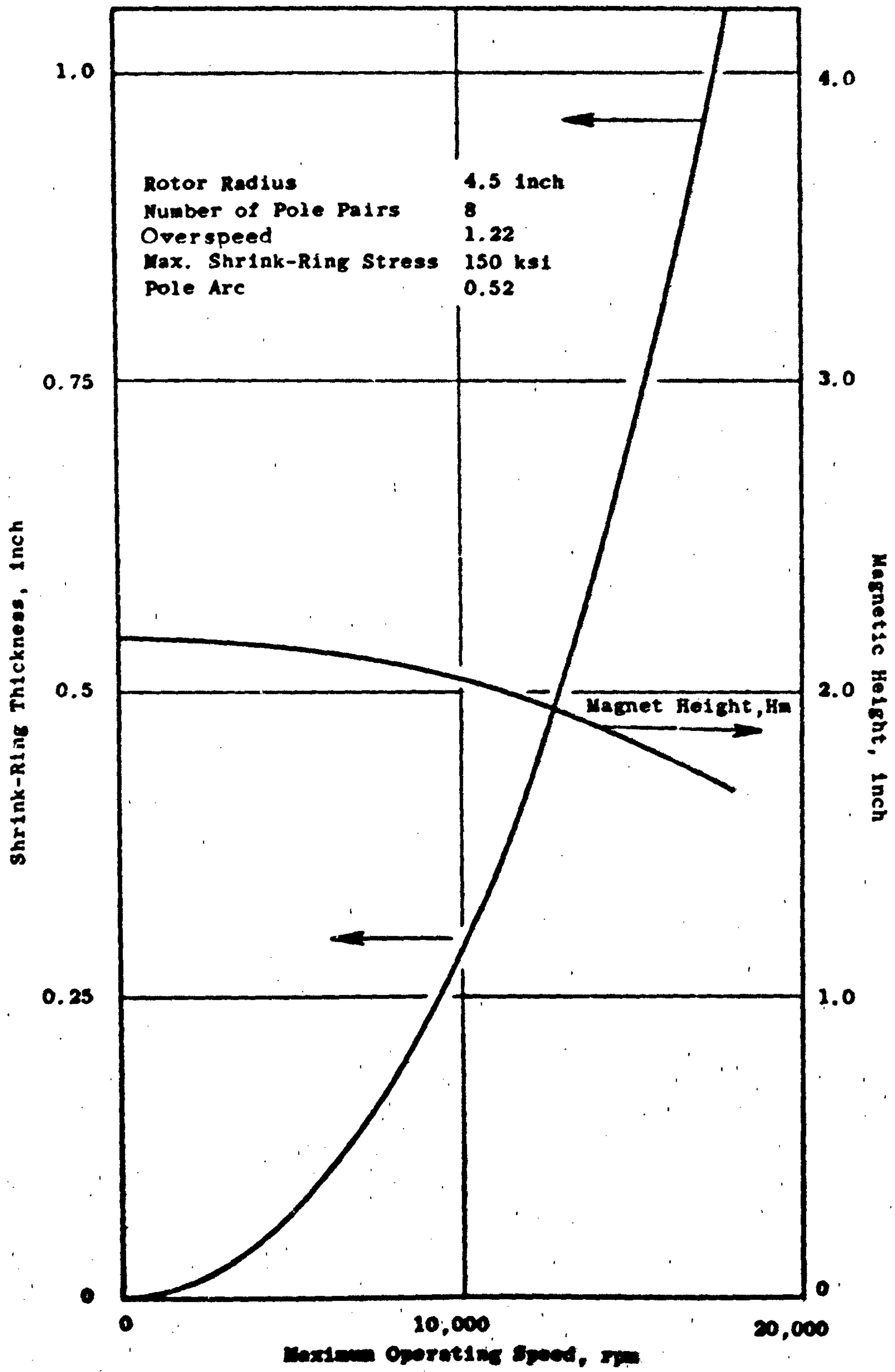


Figure 12. Shrink Ring Thickness and Magnet Height versus Maximum Operating Speed of a PM Rotor.

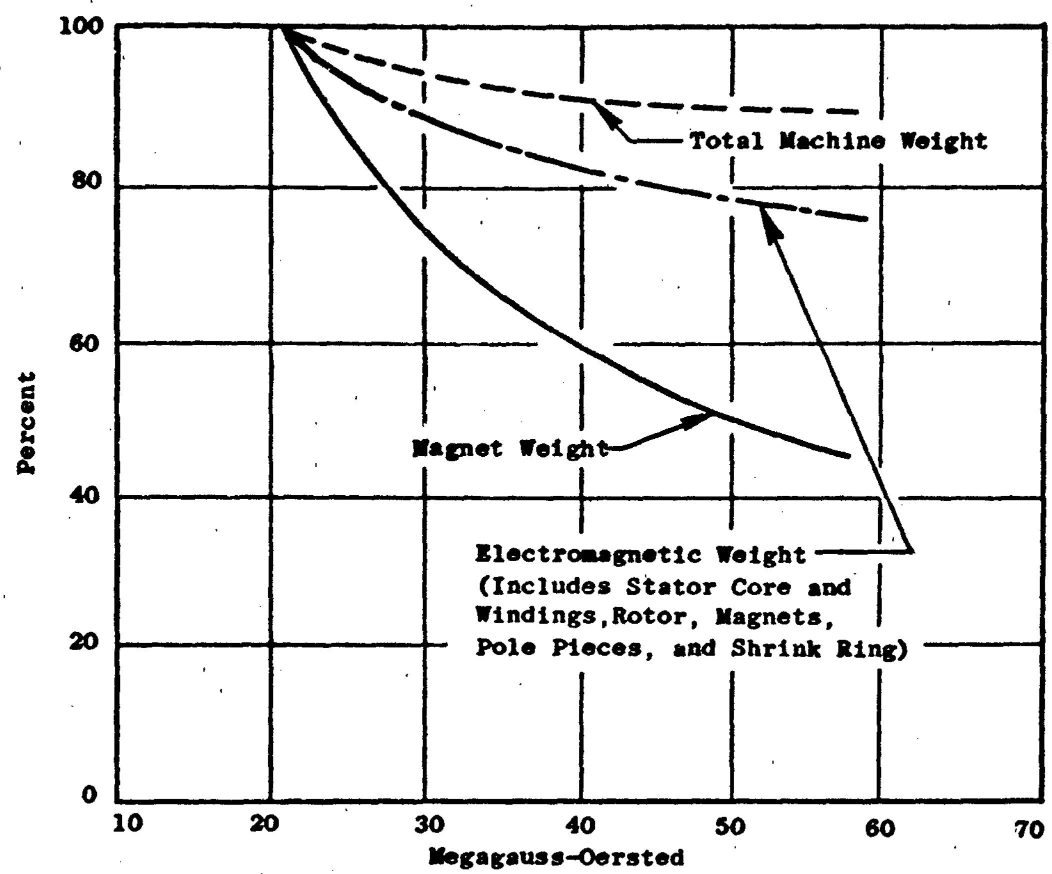


Figure 13. Machine Weight versus Magnet Energy for Cylindrical Machines.

1. It reduces the mechanical containment problems in the rotor.
2. It allows an increase in the machine operating speed and thus a reduction in total machine size and weight by means of the machine operating speed increase.

Figure 14 shows similar curves for a particular machine design (60 KVA system for TF34 application). The electromagnetic weight, the commutating inductance, and the magnet height in radial direction are shown as a function of magnet energy. Again, the effect on the rotor is significant since a lower magnet height allows a thinner shrink ring. The reactance decreases because of the change in magnet cross-section - for a PM machine the synchronous reactance is a function of both the air-gap permeance and the permeance of the magnet space.

The curves of Figures 13 and 14 have been established by keeping the flux density in the air gap constant since this basically leaves the rotor unchanged. Figure 15 presents the machine characteristics as a function of magnet energy level when magnet height and gap flux density are held constant. The figure illustrates how much more energy density can be achieved in the machine by magnets with improved energy level. The losses shown do not represent the actual losses accurately since the air-gap flux density is dependent upon pole face losses, which have been considered constant. It is expected that the total electromagnetic losses would not change significantly within the range considered, even with the pole face losses represented more accurately.

Generally, high-speed aircraft-type generators are designed for extremely high current density levels. This is done to minimize machine size and weight. The different design constraints for an integrated generator were thought to change this weight/current-density relationship. Results of tradeoff studies performed to prove this are presented in Figure 16. A current density increase of 61 percent (from 23,700 A/in.^{2} to 38,700 A/in.^{2}) results in a weight savings of 6.4 percent and a loss increase of 20 percent. In this application, therefore, very little weight and volume savings is accomplished. The weight savings is not worth the decrease in efficiency and insulation life. This also indicates that a current density reduction for increased insulation life and MTBF should not represent a significant penalty in machine size.

It is well known that the current loading of an electrical machine significantly influences the size and weight of this machine. The current loading

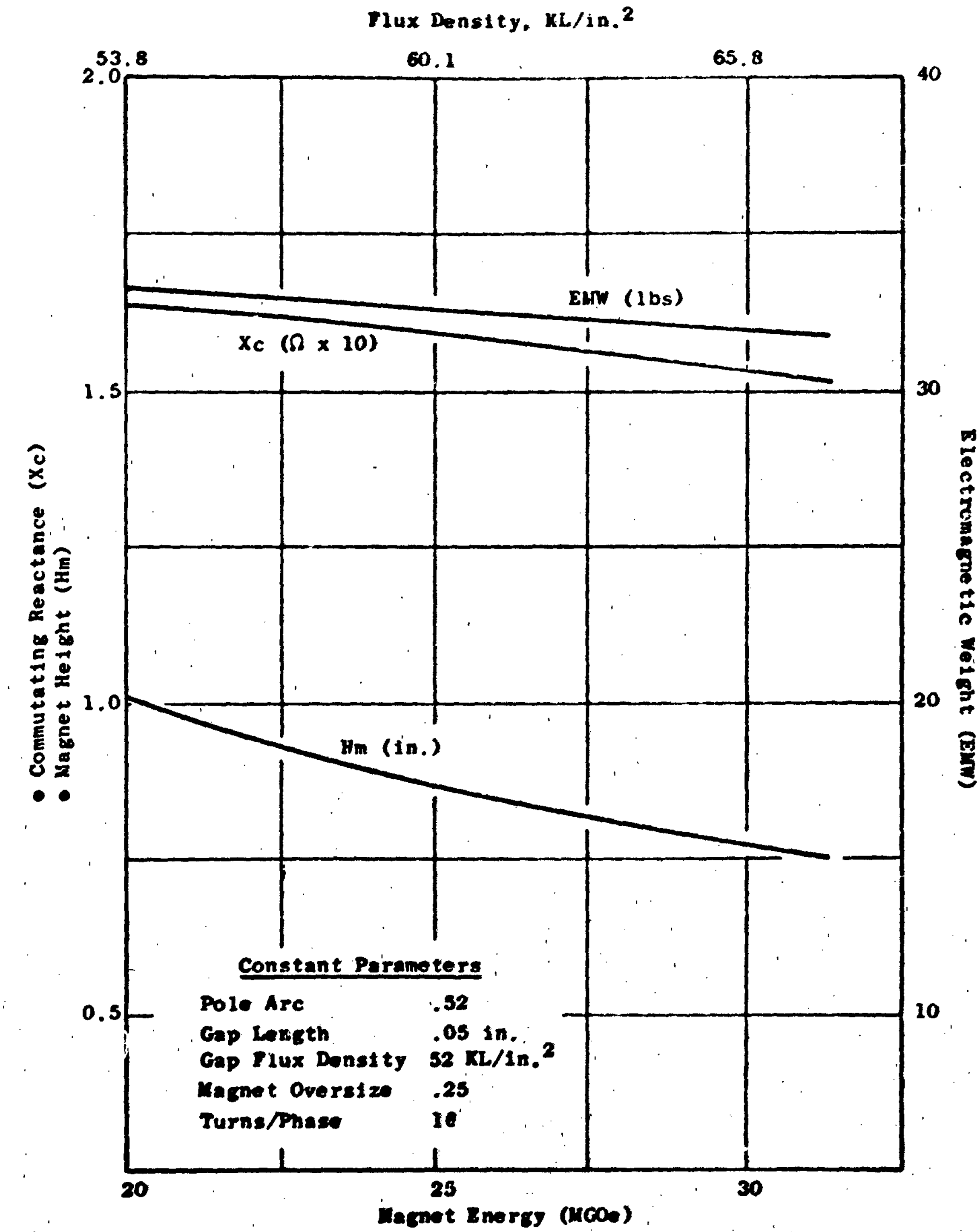


Figure 14. Machine Parameters of a Generator for a 60 KVA VSCF System Versus Magnet Energy Level for Constant Gap Flux Density.

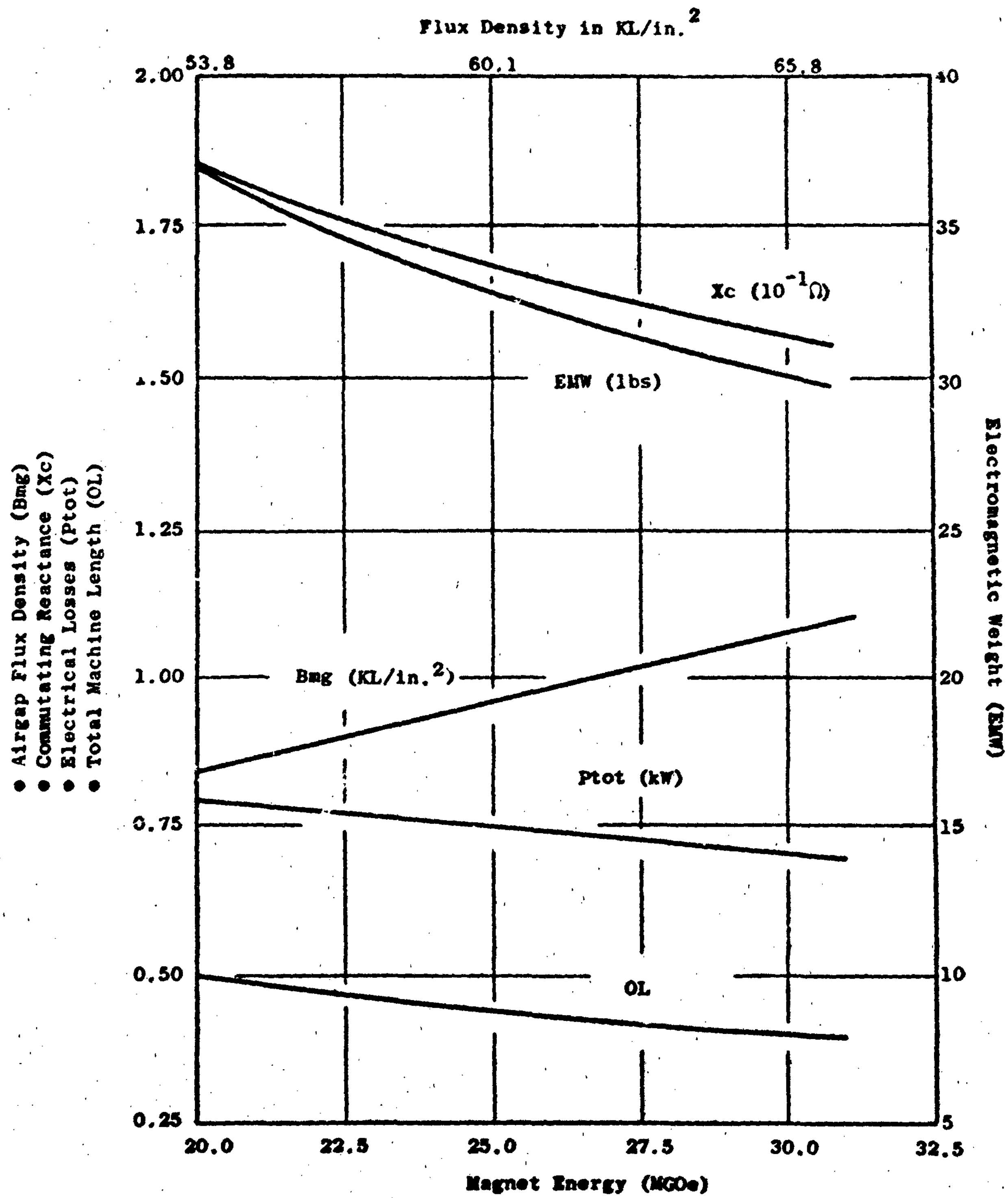


Figure 15. Machine Parameters of a Generator for a 60 KVA VSCF System Versus Magnet Energy Level for Constant Magnet Height, Losses, and Gap Flux Density at Double Load Point.

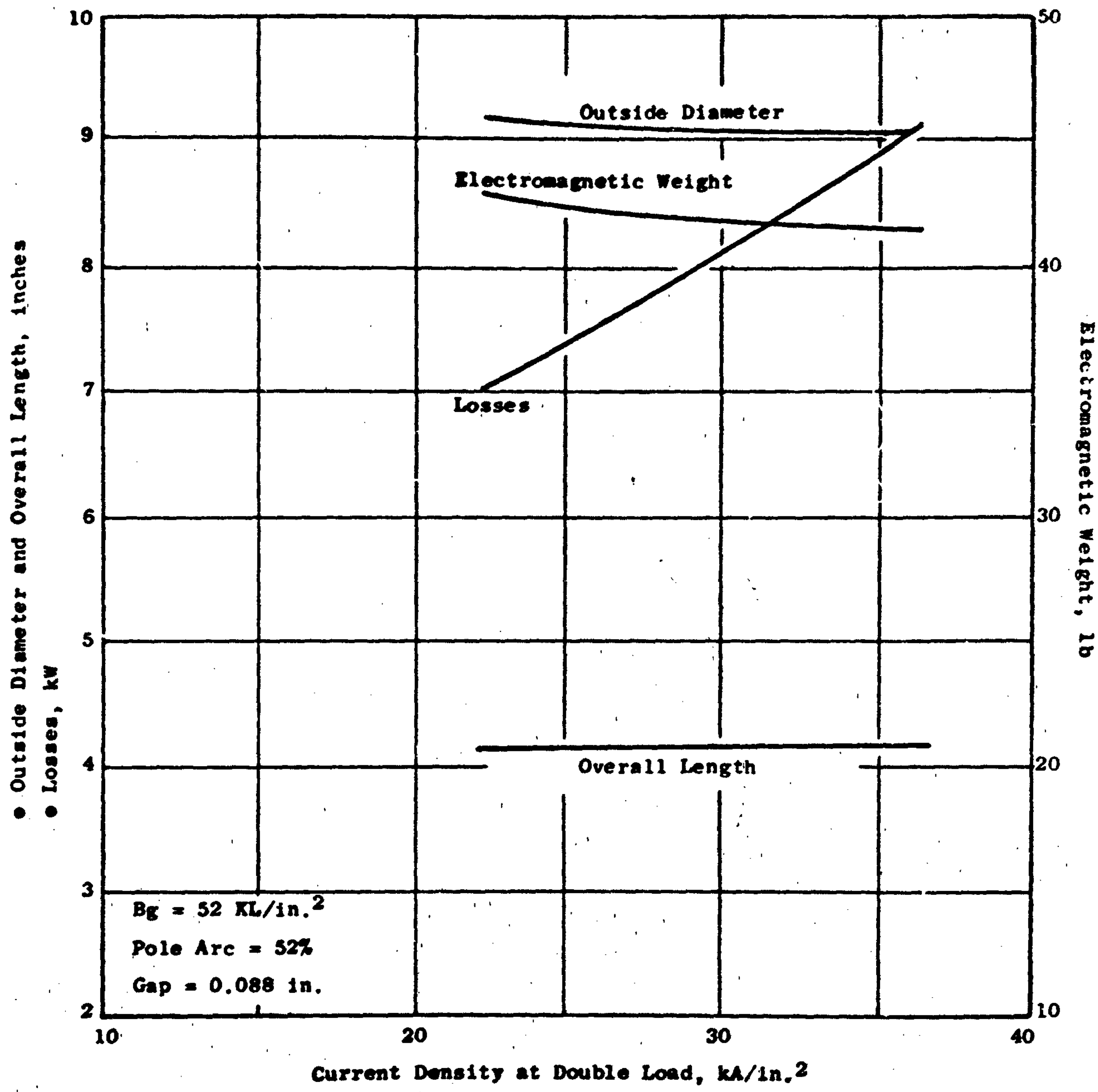


Figure 16. PM Generator Characteristics Versus Current Density for 60 KVA VSCF System.

is expressed as the total number of ampere turns divided by the bore circumference. Thus, for a given current the current loading is in direct proportion to the number of turns per phase. Results of tradeoff studies involving a change in the number of turns are presented in Figure 17. Without any amortisseur windings the change in turns per phase will directly influence the commutating reactance, as shown. As the figure illustrates, however, the increase also has a rather significant effect on the air-gap flux density because the magnet volume is limited. An increase in turns at fixed current will cause an increase in armature reactance (AR). The magnetization power provided in the magnet has to compensate for the demagnetizing effect of the AR and provide magnetization for the magnetic flux in the machine. If the AR is increased, more of the total magnetization power of the magnets is required to compensate for the AR and less is available to generate the magnetic flux. This will obviously result in a lower flux density in the air gap. Thus, an increase in current loading reduces the magnetic flux density or magnetic loading. In the range considered the electromagnetic weight still decreases as the number of turns increases. The effect, however, is relatively small: electromagnetics weight reduction is only 8 percent for a 20 percent increase in current loading. Therefore it is concluded that a further increase in turns (and current loading) in order to reduce size and weight is not practical. A size and weight reduction can only be achieved by adding an external amortisseur circuit to the rotor as described in the proposal. This is necessary to maintain the low commutating reactance required for the converter operation. It was felt that the added complexity of any amortisseur circuit is not worth the reduction in machine weight since the power losses also increase with the increase in turns per phase. Moreover, the reliability of the machine would be decreased.

Tradeoff studies of the influence of the number of poles (= frequency) are limited by two boundary conditions: (1) the minimum frequency requirement for the converter operation, and (2) the iron losses at high frequencies. The first condition requires a minimum frequency of 1100 Hz and the second condition limits the maximum frequency to approximately 3000 Hz. For the TF34 engine application the minimum idle speed of 8715 rpm will require a minimum of eight pole pairs for 1168 Hz; seven pole pairs is barely acceptable. Nine pole pairs will yield a frequency of 2522 Hz at 100 percent speed (3076 Hz at 122 percent speed). Thus only 3-pole-pair numbers are available for tradeoff studies. These tradeoff

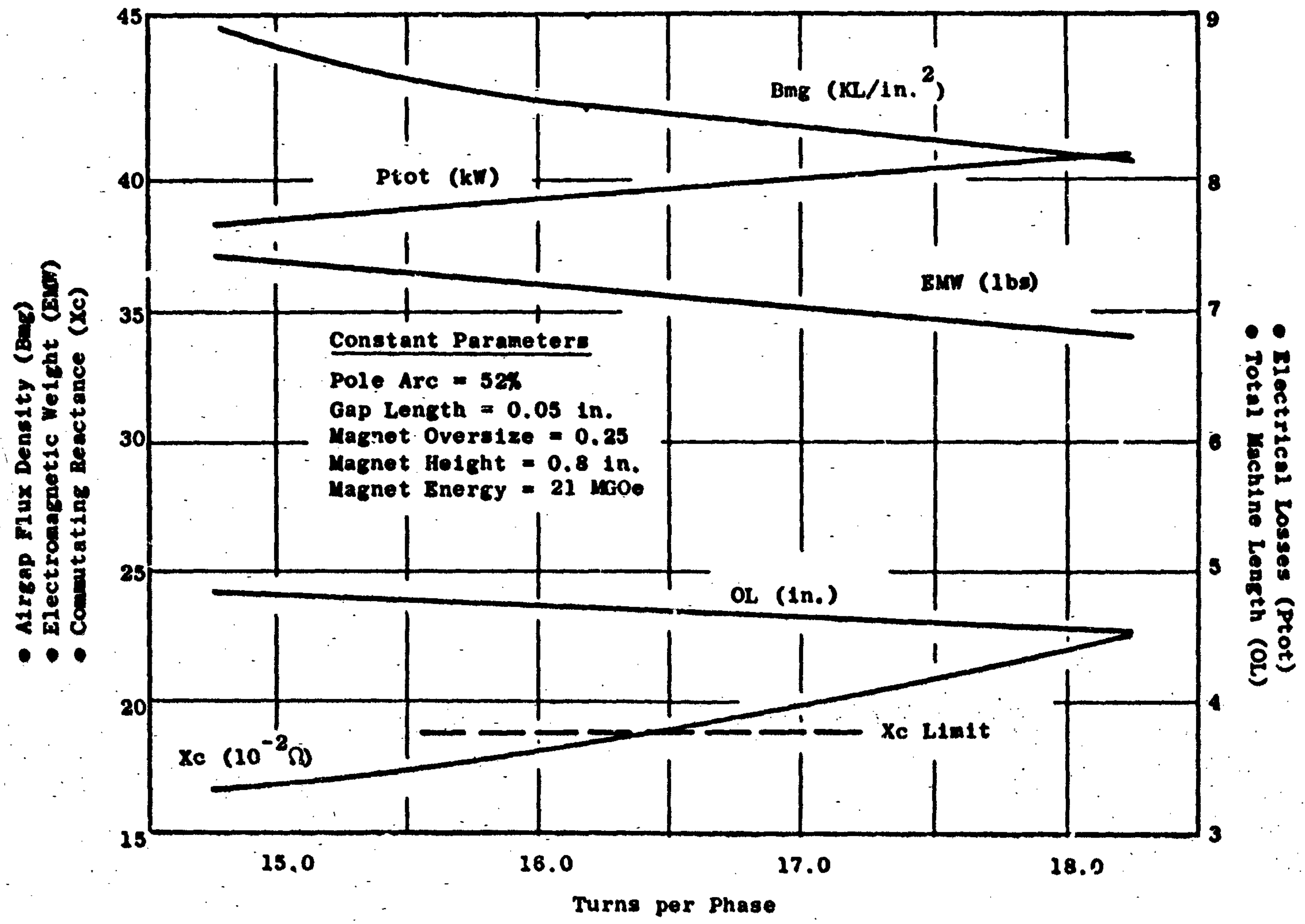


Figure 17. Machine Parameters of a Generator for a 60 KVA VSCF System Versus Turns per Phase for Constant Radial Magnet Height (Losses and Flux Density at Double Load).

studies were performed for the conditions of magnetic and nonmagnetic shaft material. The main results are reported in Table 15.

Trends evidenced from the study are as follows:

1. A seven-pole-pair machine with a magnetic shaft is not practical for this application.
2. Machines with magnetic shaft materials are larger and heavier than if they used nonmagnetic shaft materials.
3. Total machine volume and weight decrease slightly with increasing numbers of pole pairs; for the same rotor diameter the stack length over end turns decreases.
4. The losses show on the average an increase for higher numbers of pole pairs

The machines discussed above all have in common the minimum rotor inside diameter of 3.125 inches. In this case, however, the rotor geometry was quite restricted and smaller machines could be obtained by increasing the rotor inside diameter. Consequently, the rotor inside diameter was increased to 3.63 inches for this application. In addition the rotor outside diameter was allowed to vary, the additional space being taken up by a larger magnet and shrink ring. Thus, increasing the rotor diameter allows a magnet volume increase. Figure 18 shows the relationship between magnet height and rotor diameter for a maximum rotor shrink ring stress of 150,000 psi at 21,470 rpm (overspeed of TF34 engine). Using the rotor geometries defined in Figure 18, the effect of a change in rotor outside diameter was studied for the three suggested numbers of pole pairs for a 60 KVA generator for the TF34 application. The results were obtained for machine designs which met the necessary commutating reactance limits. This also meant that actual, realistic winding configurations had to be chosen. This allowed a change in the number of turns for the larger-diameter, seven-pole-pair machine.

Figures 19, 20, and 21 show the results for the seven-, eight-, and nine-pole-pair machines. The trends are clear: as the rotor radius increases, the machine length comes down while the magnet height, outer machine diameter, and weight increase. These curves allow tradeoff studies of the added engine weight for a decrease in generator length versus the reduction in generator weight for an increase in length.

TABLE 15

RESULTS FOR SELECTED MACHINES FOR THE TF34 APPLICATION

Rating = 60 KVA   Base Speed = 11,400 rpm

Shaft Material	Magnetic		Yes	Yes	No	No	No
Number of Pole Pairs	P	-	8	9	7	8	9
Base Frequency	F	(Hz)	1520	1710	1330	1520	1710
Number of Phases	-	-	6	9	6	6	9
Magnet Height	Hm	(in.)	0.803	0.800	1.22	1.170	1.190
Shrink Ring Thickness	THr	(in.)	0.331	0.245	0.445	0.450	0.450
Commutating Reactance	Xc	(Ω)	0.183	0.287	0.188	0.180	0.281
Stack Length	Hi	(in.)	3.58	2.95	2.60	2.590	2.02
Length Over End Turns	OL	(in.)	4.77	3.89	3.97	4.20	3.50
Maximum Diameter	OD	(in.)	8.72	7.92	9.00	8.94	8.97
Rotor Diameter	OR	(in.)	7.428	6.690	7.620	7.620	7.620
Rotor Inside Diameter	IR	(in.)	3.125	3.125	3.125	3.125	3.125
Rotor Weight	WR	(lb)	27	18	25	25	20
Total Weight	WT	(lb)	42	31	40	33	27
Electrical Losses (at Overload)	PT	(kW)	7.9	8.2	6.9	6.9	7.6
Air-Gap Flux Density	Bmg	(KL/in.^{2})	43	44	52	52	52

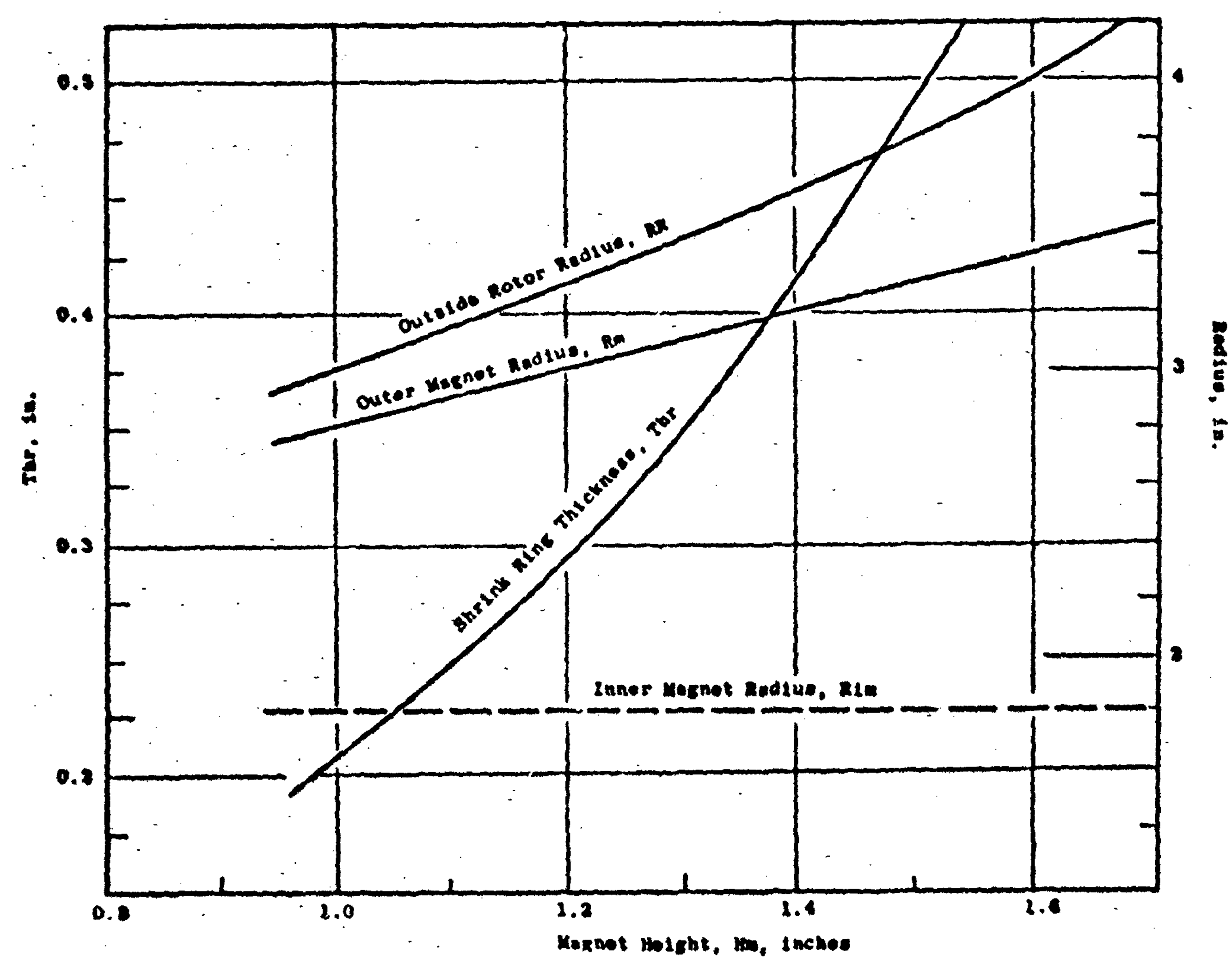


Figure 18. PM Rotor Dimensions Versus Magnet Height (Hm) for an Inner Radius for the Magnets of Rim = 1.815 inches.

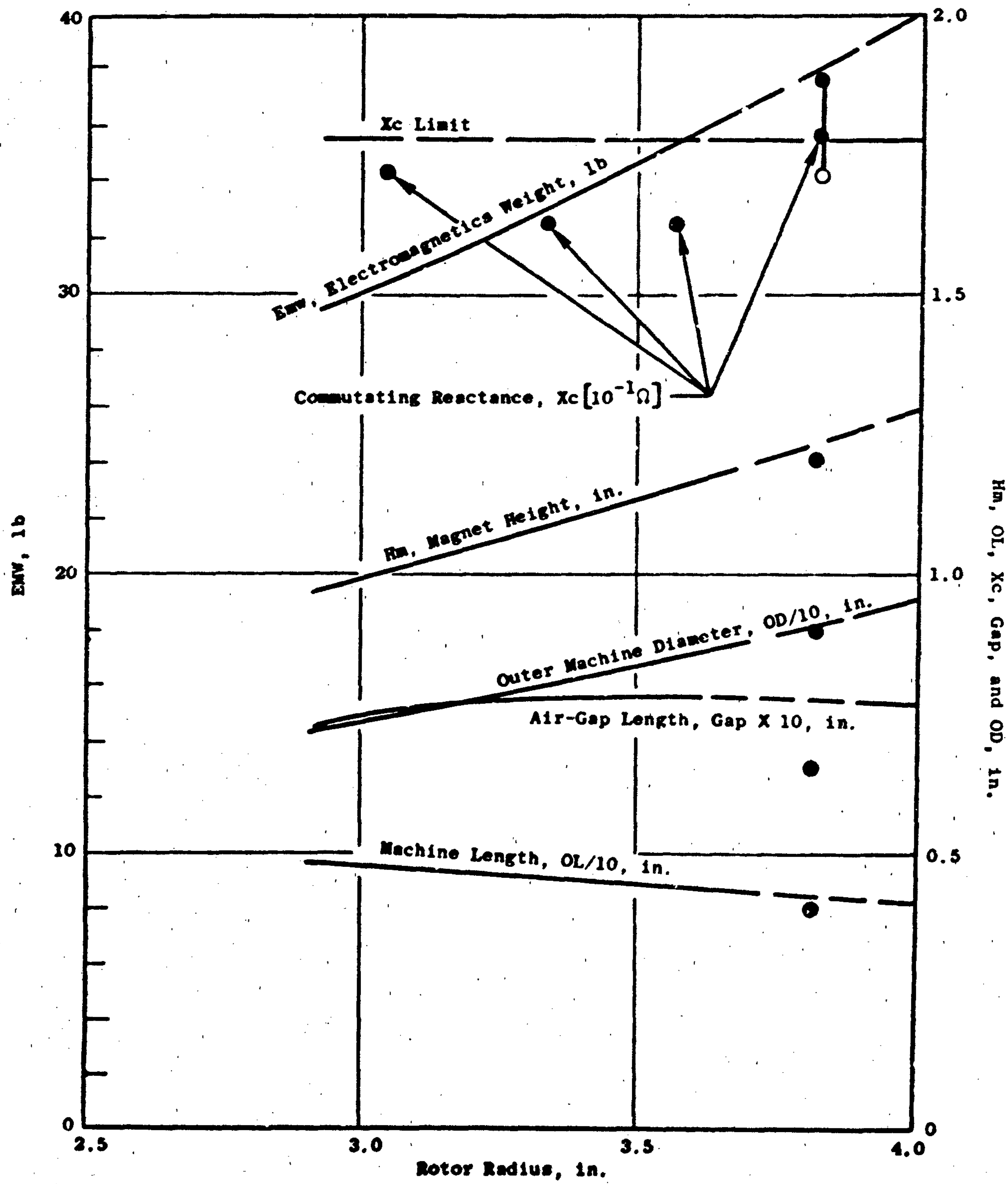


Figure 19. Machine Characteristics Versus Rotor Radius for PM Machine for TF34, 60 KVA System, 7 Pole Pairs.

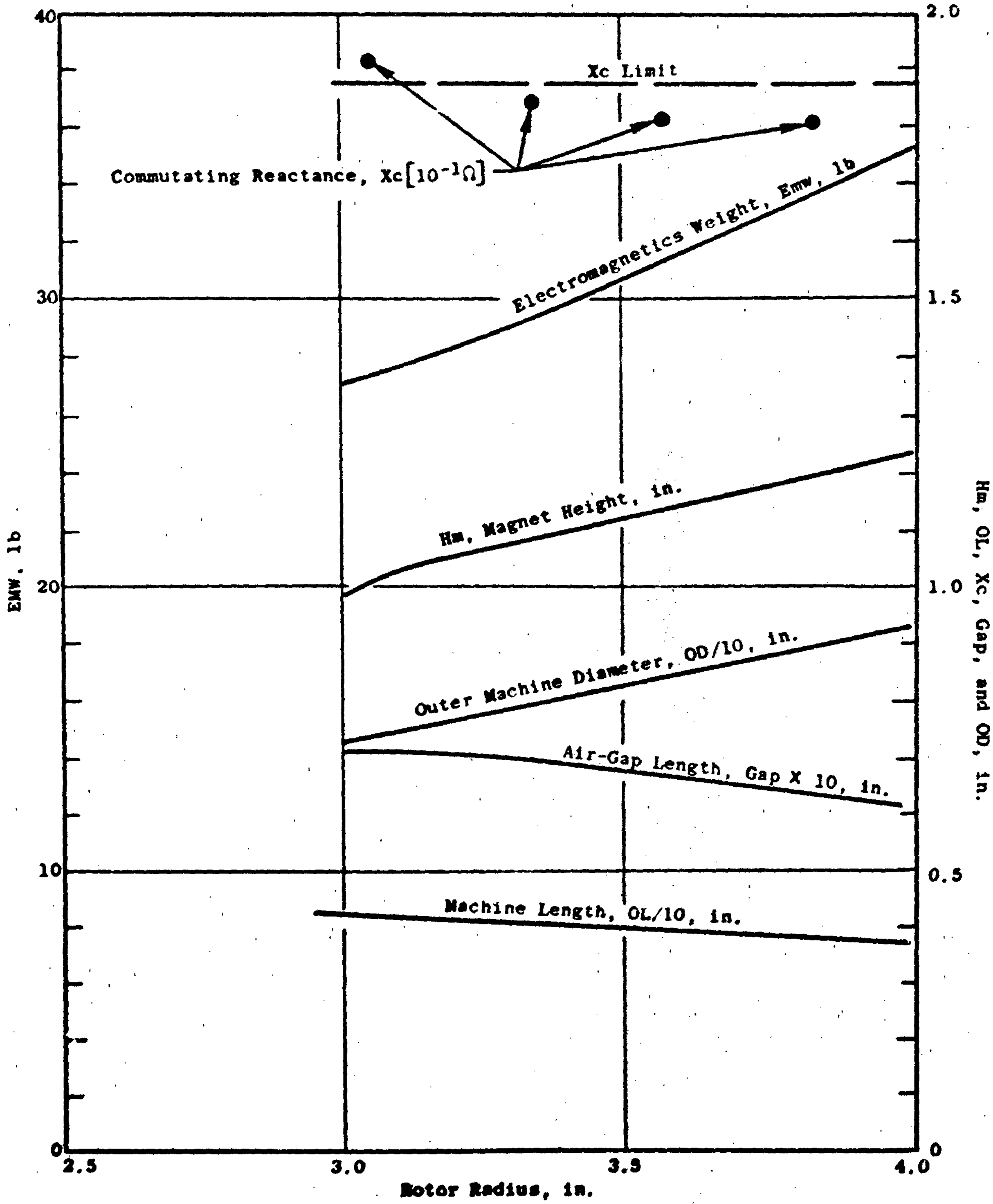


Figure 20. Machine Characteristics Versus Rotor Radius for PM Machine for TF34, 60 KVA System, 8 Pole Pairs.

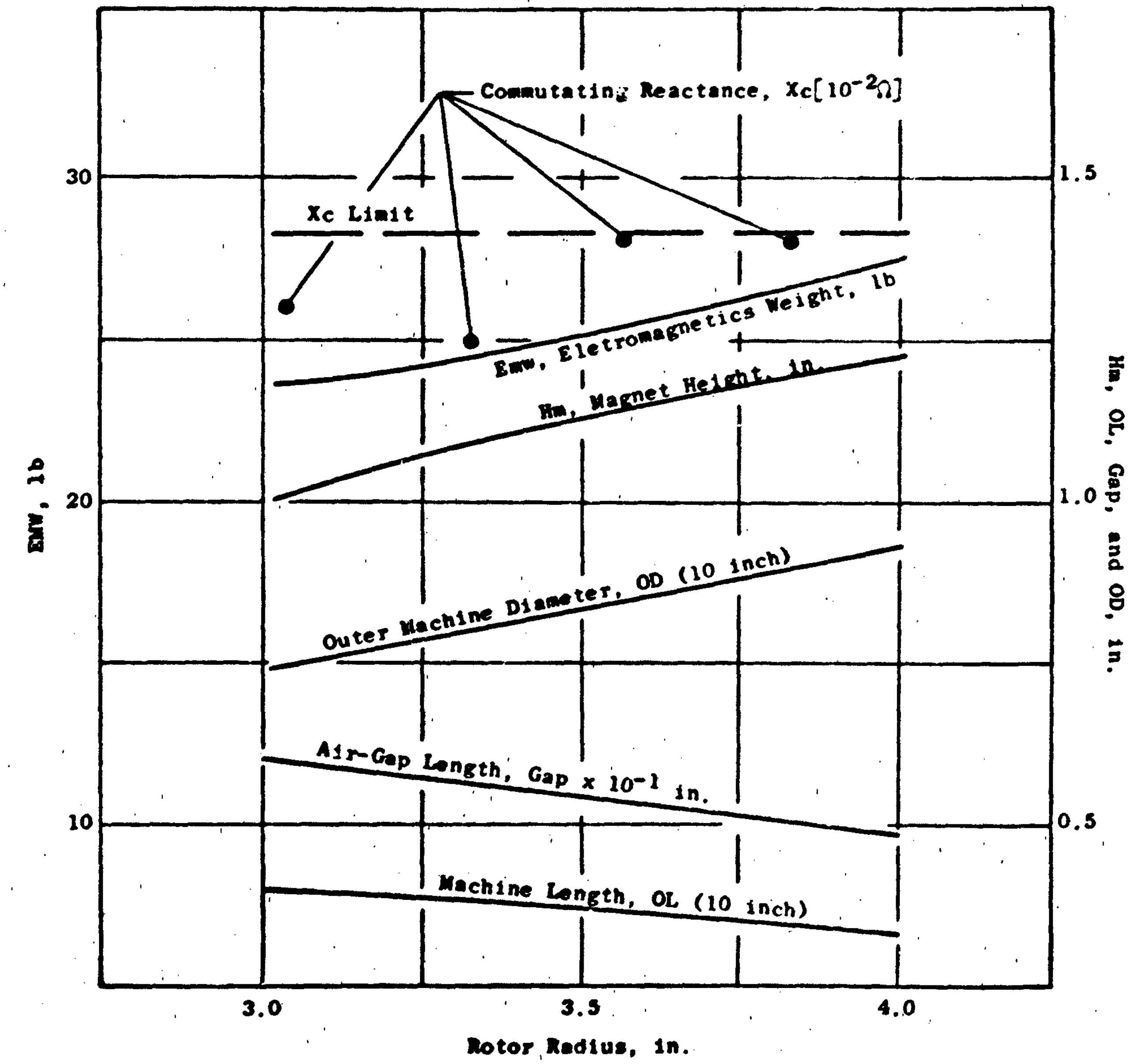


Figure 21. Machine Characteristics Versus Rotor Radius for PM Machine for TF34 Application 60 KVA, 9 Pole Pairs, 9 Phases, Nonmagnetic Shaft.

In addition to the tradeoff studies, which were undertaken to learn more about parameter sensitivities, an attempt has been made to better define the eddy current losses in the solid pole faces.

Electrical losses in the surfaces of solid rotors of synchronous machines are always caused by magnetic-field components in the air gap of these machines which rotate with speeds that differ from the rotor's. These magnetic-field components, which can have the same pitch as the fundamental, are classed as time harmonics and space harmonics. The time harmonics are caused by harmonics in the applied voltages and currents, which are introduced by the load or power supply to which the machine is connected. The space harmonics are caused by the presence of slots in the stator of the machine and also by the concentrated distribution of the armature currents in the stator slots. Each harmonic air-gap field component can be caused by one, two, or all three of the above. That means one has to investigate all three sources for possible contributions to the field component under investigation.

Time harmonics, winding distribution, and slotting harmonic fields were investigated. The magnetic flux density waves in the air gap due to time harmonics caused by the switching in the converter are difficult to establish: they continually vary because of the changes in load conditions and the beat frequency between input and output frequency. An approximation of the time harmonic amplitudes in relation to the fundamental can be obtained from the ratio of RMS current to fundamental current. A sample calculation has shown that the flux density waves in the air gap due to time harmonics currents are very small (<100 gauss) and thus cause relatively low losses.

The winding distribution harmonics were also found to generate magnetic field waves of no more than 200-gauss amplitudes. Again loss contributions turned out to be rather low. These calculations were done for the various 60 KVA machines for the TF34 application. Relatively low amplitudes for magnetic fields caused by either current (time) harmonics or winding distribution are to be expected for machines with low armature reaction, which is identical to low synchronous reactance. Since these machines have been designed for a small commutating reactance without the benefit of amortisseur windings, the synchronous reactance had to be made small. (Xd is generally below 0.4 per unit for these machines.)

The analysis of the slotting harmonics shows rather significant flux density amplitudes for all three machines (seven, eight, and nine pole pairs). Table 16 shows the amplitudes for the first three pairs of slot harmonics. Table 17 shows the losses in the rotor surface for the first pair of slot harmonics.

These losses were calculated for a uniform rotor material using the theory for solid-rotor induction motors with magnetic rotor material. In the actual rotor surface we have two distinctly different types of material present: magnetic and nonmagnetic. An actual analysis for that combination is not available. An inspection of the formulas used suggests the following: In the nonmagnetic zones the depth of radial current penetration in the material is larger than for the magnetic zones. The assumption of radially constant flux densities is not valid anymore, and the amplitudes of the harmonics will be significantly reduced. For lack of better information it is assumed that the two effects cancel each other. This seems justified, because the loss levels calculated relate well to those experienced in no-load tests of a permanent magnet generator built for a 150 KVA VSCF system.

The basic difference for the three machines considered is in the actual frequencies as determined by the number of slots and the fundamental frequency. Higher frequencies are better damped for the set of material characteristics and thus cause smaller losses. In addition the eight-pole-pair machine has a larger ratio of slot opening to air gap, resulting in larger flux density amplitudes.

The basic conclusion that can be drawn from these loss calculations is that slotting harmonics are the main cause of the losses in the rotor surface. High numbers of slots and poles seem to result in somewhat lower losses because of better damping. Open slots or large slot-opening-to-air-gap-length ratios result in increased losses. The limit in slot opening is basically determined by manufacturing considerations.

One can briefly summarize the results of this part of the studies as follows. The general tradeoff studies have shown the significance of rotor optimization with respect to its two main functions: (1) "production" of magnetic fields, and (2) mechanical containment of the magnetic structure. This is expressed in magnet energy level, magnet volume, and shrink ring (retaining ring) thickness. However, it has also been shown that once the electromagnetic loading in the air gap has reached the limit established by the stator materials and

TABLE 16

AMPLITUDES OF SLOT HARMONICS

Flux Density	Harmonic Amplitude, kilogauss Seven Pole Pairs	Eight Pole Pairs	Nine Pole Pairs
B_{11}	2.467	3.083	2.467
B_{13}	2.467	3.083	2.467
B_{23}	0.221	0.315	0.221
B_{25}	0.221	0.315	0.221
B_{35}	0.169	0.338	0.169
B_{37}	0.169	0.338	0.169

TABLE 17

CALCULATED LOSSES DUE TO THE FIRST PAIR OF SLOT HARMONICS

	Seven Pole Pairs		Eight Pole Pairs		Nine Pole Pairs	
Slot Harmonic ν	11	13	8	10	11	13
Losses at 11,400 rpm per cm length, W/cm	18.3	25.8	29.5	46.7	16.2	22.9
Loss Density at 11,400 rpm, W/in.^{2}	4.68		8.09		4.15	
Total Losses at 11,400 rpm, W	292		500		200	
Total Losses at 17,600 rpm, W	556		925		382	

thermal characteristics a further increase will simplify the rotor design but not influence the total machine size. Current density increases do little to reduce the machine size. For improved reliability the current density level should be kept lower than is generally done for aircraft-type generators.

Voltage regulation difficulties, commutating reactance requirements, the large magnetic volume required to compensate for armature reaction, and the complexity and reliability concern of an amortisseur circuit lead to the conclusion to eliminate the amortisseur winding from consideration. Any size and weight benefits appear to be marginal for machines to be integrated into engines compared to the sacrifice in reliability in a very critical location.

The electrical rotor surface losses for PM machines with a low reactance level have their main origin in the stator slotting harmonics. Special attention is to be given to the slot opening design to minimize these losses.

C. DISK-TYPE MACHINES

The most likely alternate PM machine for the integration application is the disk or axial-gap machine. This concept, which is relatively new in power generation machinery, is presented in Figure 22. The winding disk is mounted to the stator structure; it is manufactured from copper windings which are reinforced with fiberglass and impregnated with epoxy resin. Coolant channels and additional support structure would be embedded, if the application demands such devices, within the copper winding. The winding disk is faced on both sides with rotating magnet disks. The polarity of magnets alternates, as shown in the lower right of Figure 22. The magnets are backed by magnetic steel plates and supported on the inside and outside by nonmagnetic support bands. The magnetic back plates can be made relatively thin because of the lower operating flux density levels in the magnets and the high flux density capabilities of the magnetic steel. If it is required to multiply the output power, two or more of these disk machines can be stacked one behind the other; the number of disks that can be stacked depends upon the engine configuration and the associated payoff.

The disk machine concept, in which basically the soft magnetic iron has been replaced by permanent magnets, permits size and weight savings over certain other electrical machine concepts because the ratio of active volume to passive machine volume has been significantly improved. This concept also has the potential for very low losses, since the only electromagnetic losses present are the I^2R losses in the rotor windings.

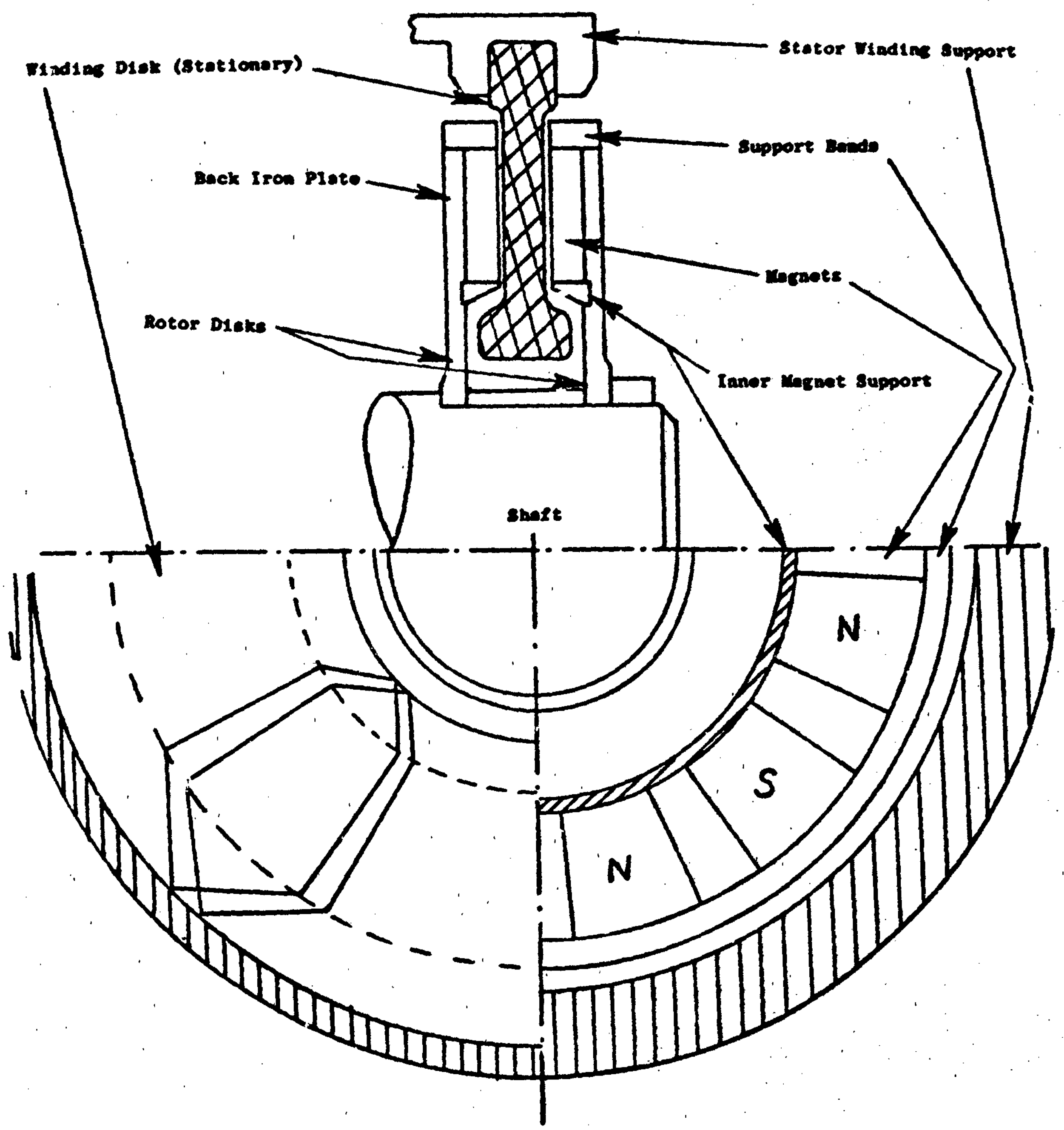


Figure 22. Disk-Type PM Alternator (Cross Section).

In addition to the potential weight savings for general machine applications, there are other, indirect advantages to the axial-gap, disk-type machine. The rotor manufacturing techniques, close tolerances, and level of quality control are similar in both cases but the retaining ring for the disk PM is made from a single nonmagnetic material rather than fabricated from two dissimilar metals as required for the radial-gap (conventional) machine.

Also, the effects of misalignment of the axial-gap-type machine are significantly different from what they are on the conventional machine. Since there is no iron in the stator of the disk, any misalignment or eccentricity will not cause unwanted magnetic forces or additional losses. Radial clearances in the range of 0.10 to 0.20 inch and eccentricities up to 0.050 inch can be tolerated. For mechanical (rubbing) reasons, axial clearances are more critical than for the cylindrical PM machine. The magnetic influence of axial misalignment is insignificant. Thus, for the axial PM alternator the assembly tolerances are significantly less critical than they are for a conventional PM machine.

Due to the radical change of this type of machine from the conventional radial-gap-type machine, it is expected that a considerable amount of development will be necessary before an axial-gap, disk-type machine can be realized. But because of its potential advantages it has been included in the studies. A cross section showing the mechanical design features of a disk-type IEG/S PM machine is shown in Figure 23.

Except for the magnet axis and the pole pieces, the mechanical design of the rotor is similar to that of the cylindrical machine. For example, a shrink ring holds the magnet assembly (magnets and nonmagnetic spacers) under compression up to the maximum overspeed condition. The manufacturing of this shrink ring, however, is simplified since it is made from a single, high-strength, nonmagnetic material. The relationships between radial magnet dimensions, rotor diameter, and shrink ring thickness (radial) are similar to those established for the cylindrical machine. This is obvious from Figure 24, in which is shown the dependence of the shrink ring radial thickness on the outer magnet radius and on the materials selected for spacers and shrink ring. Since the inner magnet is fixed, these curves also show the dependence of the ring thickness versus radial magnet height. Geometrically, the optimum machine utilization occurs when the ratio of magnet outside radius to inside radius is $\sqrt{3}$. However, at the speeds considered (TF34 application) this rotor radius ratio is not practical: a

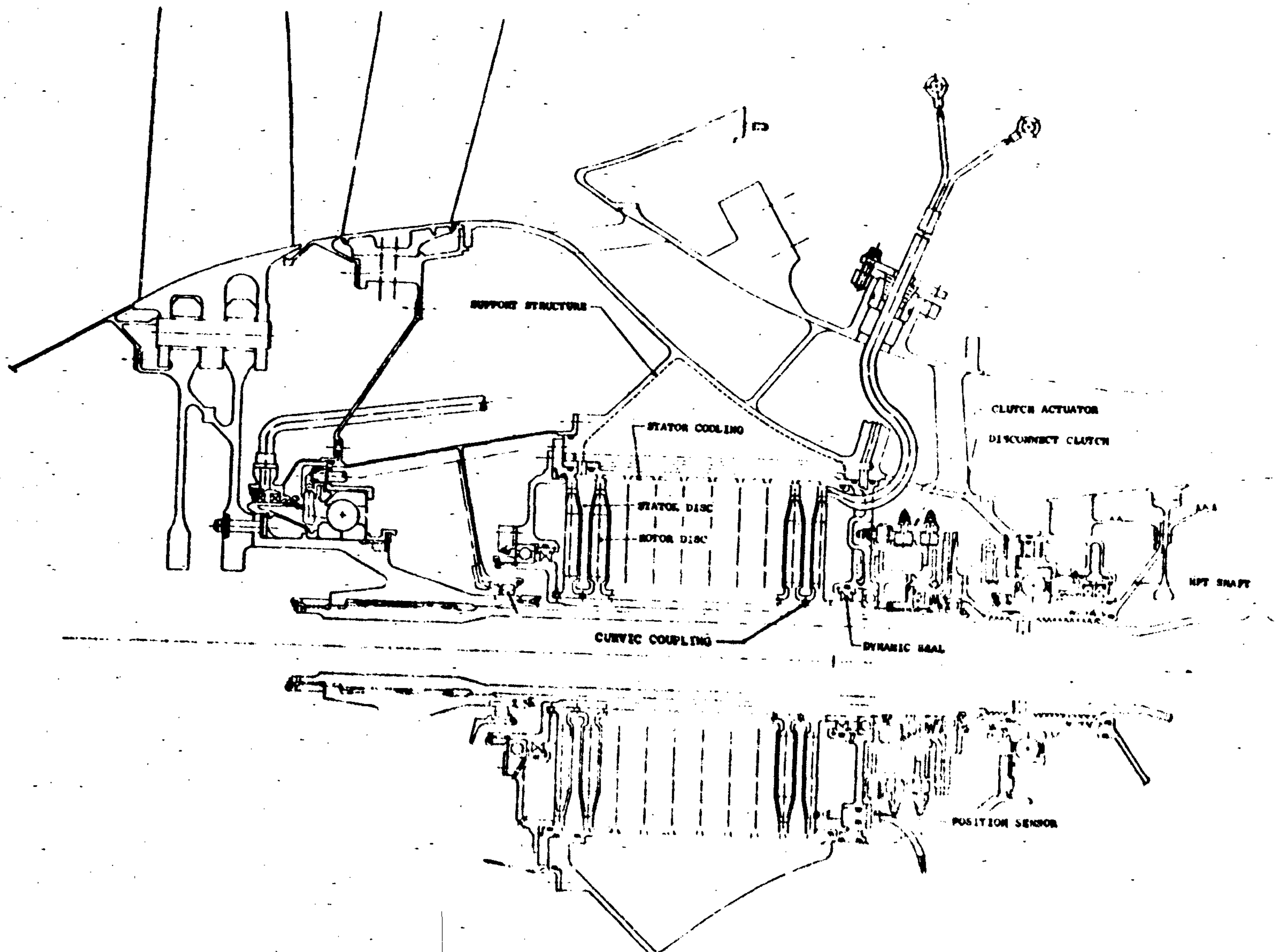


Figure 23. Typical Multidisk IEG/S PM Machine (TF34, 120 KVA).

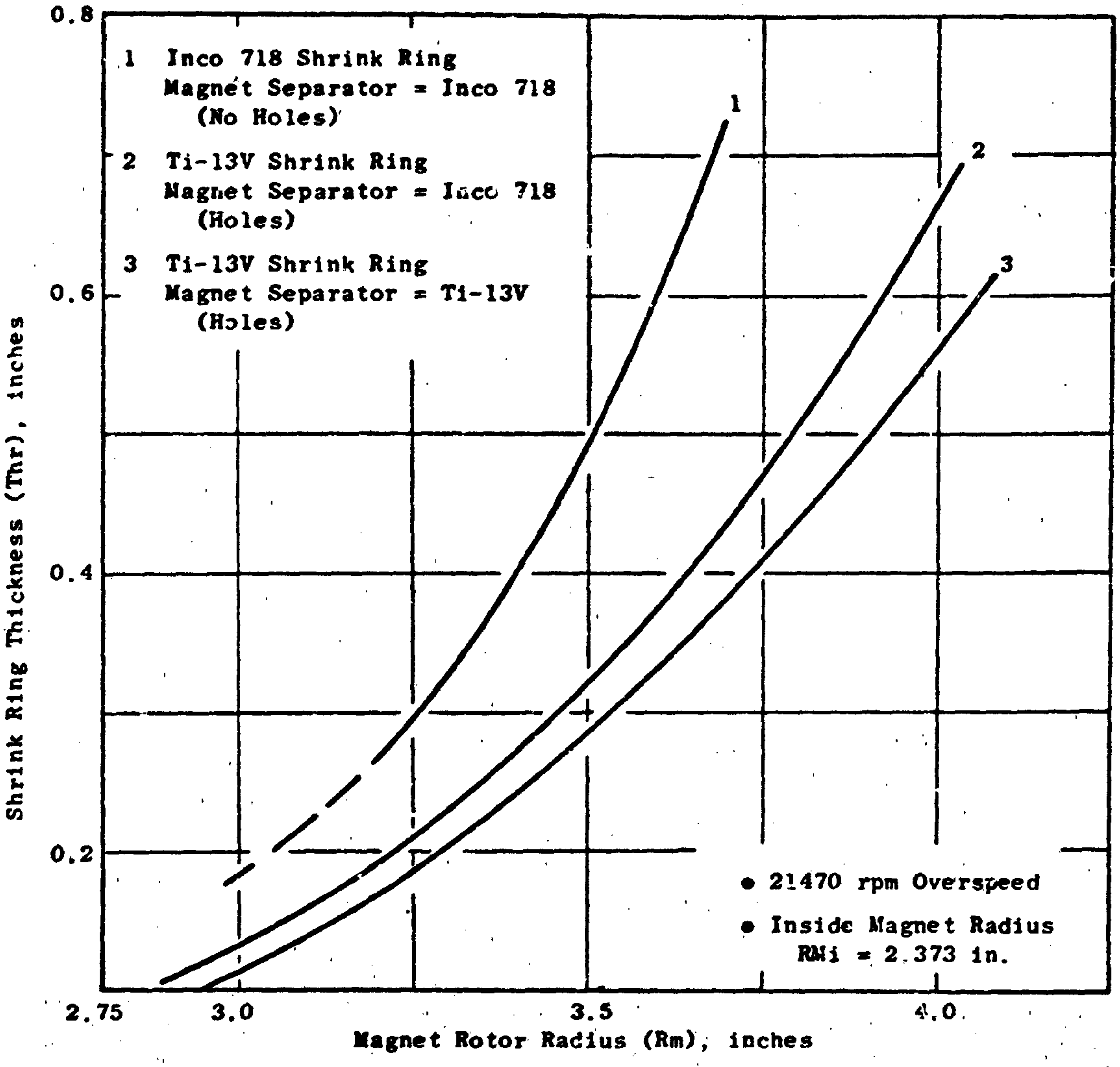


Figure 24. Shrink Ring Thickness Versus Magnet Rotor Radius.

reduction of the inner magnet radius is hindered by the inner end-turn configuration and the shaft diameter.

The radial dimensions have not been considered as sensitivity study parameters because (1) the magnet inside diameter is fixed due to shaft diameter, and (2) the outside diameter is optimized when the diameter ratio $(r_a/r_i) = \sqrt{3}$.

The results of current loading tradeoff studies are shown in Figures 25 and 26. The axial magnet thickness is generally chosen so as to obtain a magnet operating point close to the maximum energy point. This will result in machines close to the minimum volume, since the magnet length is a direct input to the total volume equation. However, some tradeoff studies have been performed to study the trends.

For Figure 25 the following parameters were considered to be constant:

<u>Constant Parameters</u>

Permanent Magnet Magnetization	0.85 Wb/Eq.M.
Permanent Magnet Pole Arc	60 Electrical Degrees
Axial Gap (Magnet-Armature)	0.1 inch
Armature Current Density (Copper)	15,000 amp/in.^{2}
Armature Winding Space Factor	0.5
Overall Outer Diameter	9.05 inches
Minimum Inner Diameter	3.25 inches
Operating Speed	11,400 rpm
Generator Output	100 KVA
Power Factor	0.75 Leading

The armature current loading is defined as follows:

Armature Current Loading (amp/in.) = Current Density x Space Factor x Armature Axial Thickness. The independent variable is the armature current loading, and the dependent variables are: number of disks, synchronous reactance per unit, and active length. The magnet axial thickness was chosen at 0.8 inch for these curves. Two sets of curves are shown, one for 16 poles and the other for 12 poles.

These curves define the size of the machine through the active length, since the outside diameter is constant. The synchronous reactance is considered to be a sensitive parameter in this application, since one operational requirement is a low commutating reactance. The commutating reactance in a conventional synchronous generator is approximately the negative sequence reactance, which,

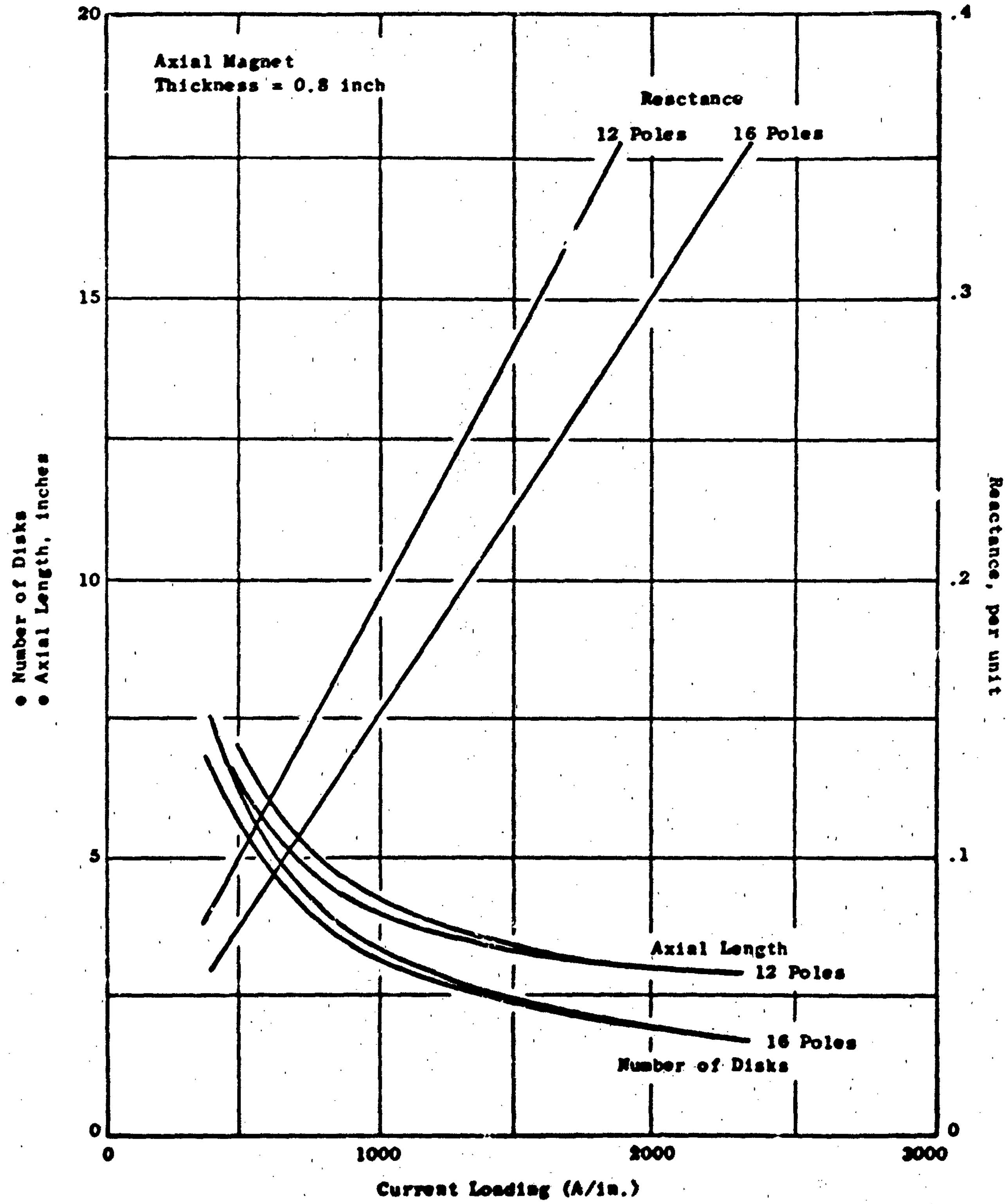


Figure 25. Disk-Type PM Machine Parameters Versus Armature Current Loading for 12 and 16 Poles.

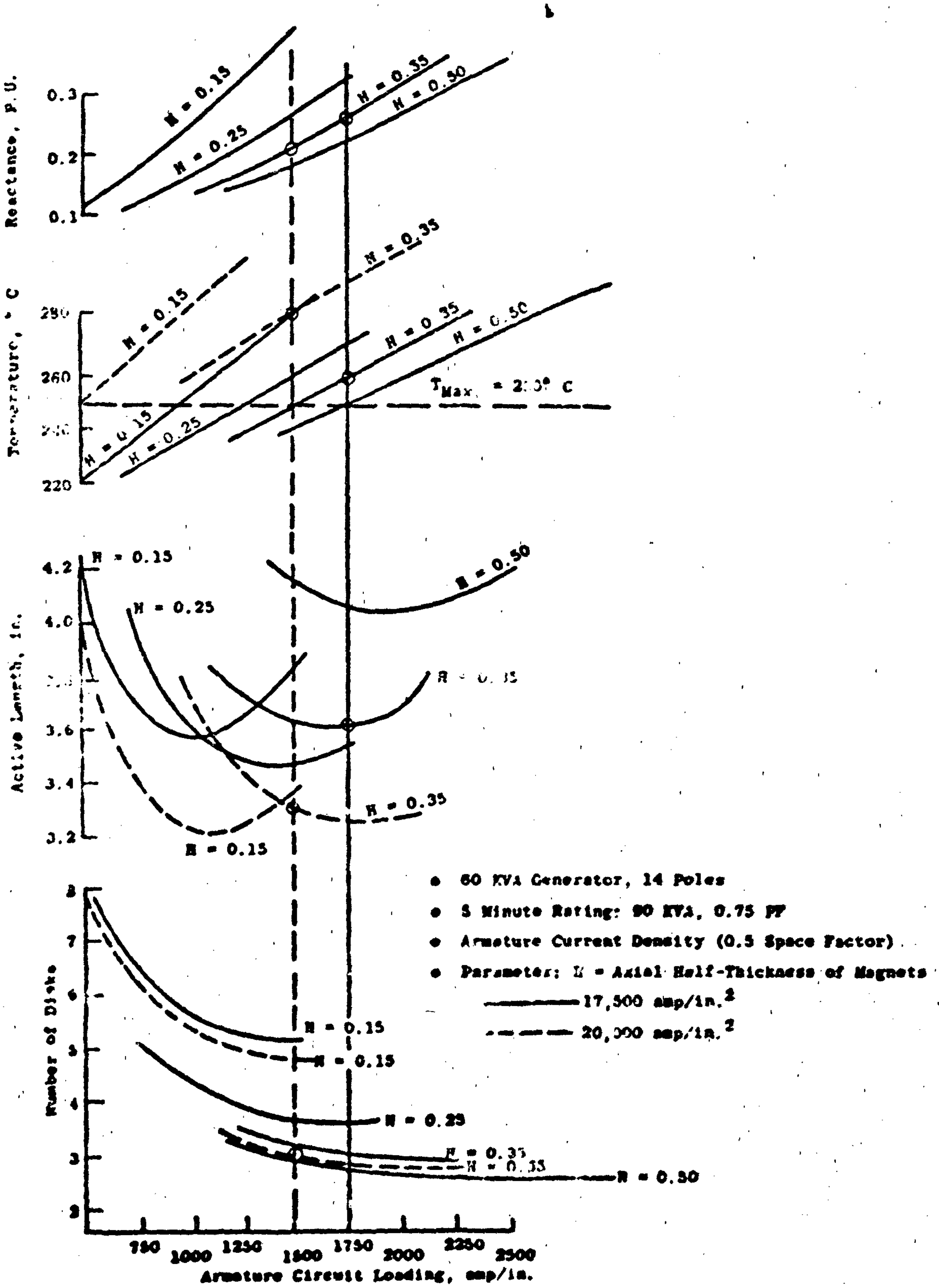


Figure 26. Machine Parameters Versus Armature Current Loading.

for practical purposes, is the subtransient reactance. In the disk machine there is only one reactance, since the electrical resistivity of the magnets is so high that pole face currents are negligible. An amortisseur winding could be added in the form of copper disks on the pole faces. Previous experience with air-gap stator windings (such as the ones used here) shows that if such a winding were added, the subtransient reactance would be approximately one-third the synchronous reactance. A commutating reactance of less than 0.1 per unit is desirable and would imply an upper limit of about 0.3 per unit on the synchronous react :e.

. ıortisseur disks would introduce a complication into the design, and the curves in Figure 25 show that it is possible to achieve a synchronous reactance of less than 0.1 per unit without extreme penalty, especially with the 16-pole design.

The armature losses over the range of parameters explored amount to approximately five percent. It becomes apparent that the efficiency of the machine will not be limiting. However, in this configuration, extraction of the losses must be accomplished at the outer periphery of the stator disks. This heat removal capability may put a limit on the loss per disk. This will be discussed shortly.

Similar results are obtained for a smaller machine rating as applicable to the TF34 application. The dimensional limitations for this design are listed in Table 18. The results in Figure 26 are given for various values of axial magnet half-thickness.

The lowest set of curves in Figure 26 shows the number of disks required (twice the number of half-disks). Although these curves are continuous, it is obvious that only integer values are physically meaningful. The next set of curves shows the machine active length. The next set of curves shows the hot-spot temperature in the armature windings (the 250° C limit is indicated). The basis for the temperature calculations is discussed later. The top set of curves shows the synchronous reactance in p.u. on the 90 KVA machine base. In this type of machine, the commutating reactance and the synchronous reactance are identical, and a permissible synchronous reactance on the 90 KVA base is 0.45 per unit for a nine-phase system. Therefore, the curves in Figure 25 show that this limit is not reached within other practical limitations of the machine dimensions.

The basic calculations were made for an armature copper current density of 17,500 amp/in.^{2}.

TABLE 18

MACHINE DIMENSION ASSUMPTIONS FOR 40 KVA MACHINE
OPERATING AT THE 1.5 P.U. OVERLOAD POINT

Limiting radius, outside	4.550 in.
Magnet outside radius	3.620
Magnet inside radius	2.150
TF34 shaft radius	1.562
Clearance for cooling pipes	0.25
Rotor clearance	0.10
Trapezoidal shrink ring radial height	0.58
Space for inner turns	0.238
Clearance, end turns - rotor	0.25
Rotor support cylinder	0.10

The curves for the active length exhibit minima, which are also minima in machine volume. Realizable machines are obtained for the combination of parameters that results in an integer number of disks. These vertical lines are shown for six- and three-disk machines near the active length minima. (Disk multiples of three are desirable for phase-connect purposes.) For current densities of 17,500 amp/in.^{2}, both machines have acceptable hot-spot temperatures. The machine active length is approximately 3.6 inches in both cases. For a 20,000 amp/in.^{2} current density, the active length is decreased by about 0.3 inch for the three-disk machine, but the indicated hot-spot temperatures are perhaps 20° C too high. Refinement of the thermal calculations, including verification of the assumptions therein, may reduce these predicted temperatures, but for the moment the data in Figure 26 seem reasonable.

The assumptions used in heat transfer calculations are best illustrated by reference to Figure 27, which is a full-scale cross section of the disk machine. The resistive losses in the armature winding are extracted by radial conduction along the armature winding. The heat then flows out through the end turns to a set of copper fins embedded in the end windings as shown. The internal resistance of the end windings is important, and a high-conductivity-filled epoxy has been assumed. In addition, however, the thermal conductivity of the embedded copper is taken into account, and care will be necessary to assure that the winding strands are pressed as close to the fins as possible. Heat transfer area is at a premium, and in order to maximize the surface heat flux at the fins as well as minimize the internal temperature rise, it was found necessary to split the end windings as shown in Figure 27. The mechanical realization of this heat transfer configuration will require careful design. The end winding fins conduct the heat to the oil ducts, where it is transferred by forced convection to the oil. The distribution of the temperature rise is shown in Table 19, using the three-disk machine at 17,500 amp/in.^{2} as an example.

The internal temperature rise in the windings was calculated by assuming one-dimensional heat flow in a body with internal heat generation, from which the temperature rise is given by

$$\Delta T = \frac{J^2}{2K} z^2 \qquad (11)$$

where J is the armature copper current density,

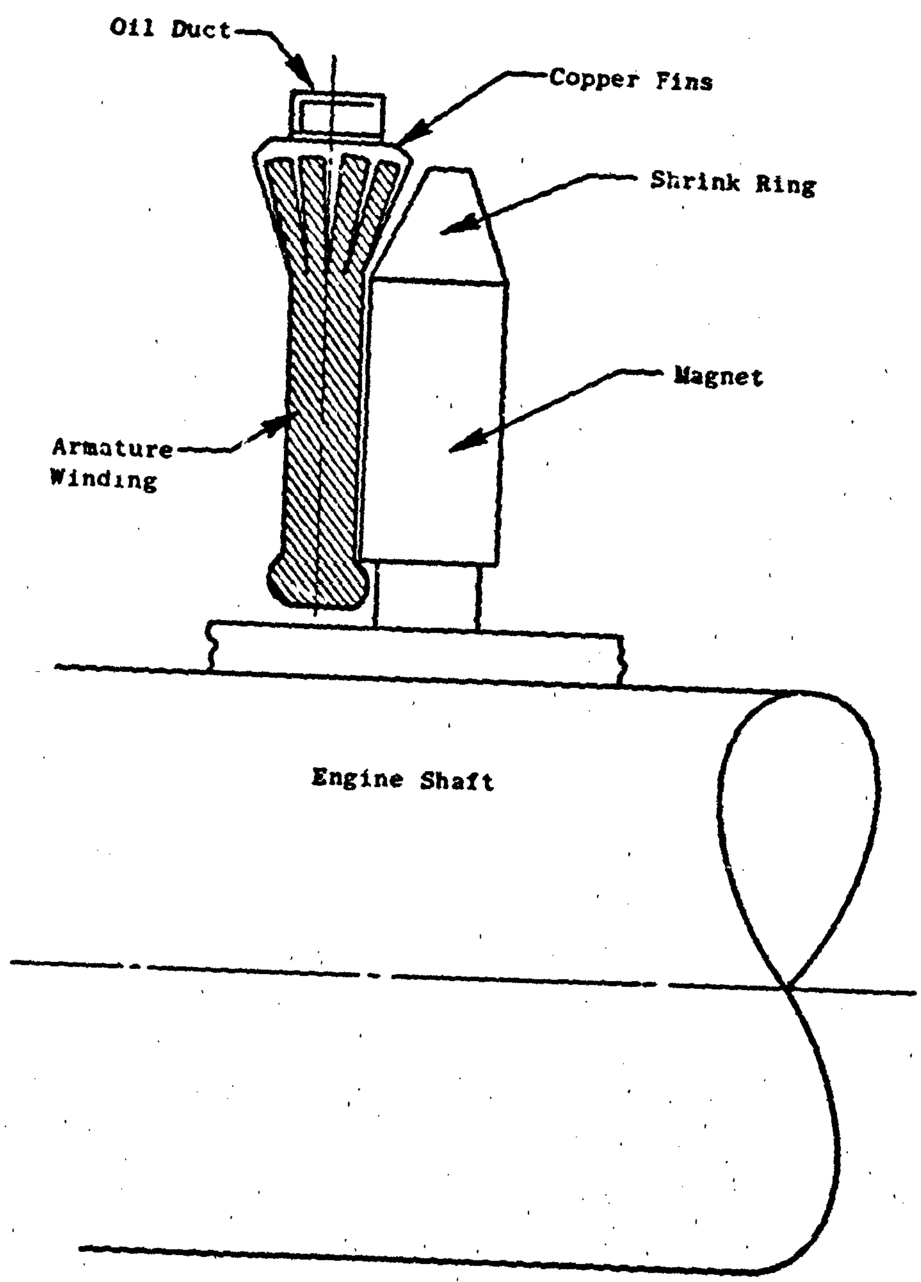


Figure 27. Disk Generator Cross Section, Full Scale.

TABLE 19

INTERNAL TEMPERATURE RISES - 90 KVA/THREE-DISK MACHINE

Armature radial conduction (current density 17,500 amp/in.^{2})	86° C
End turn internal conduction	22
Oil-duct film rise	30
Oil bulk rise (Oil flow 2.9 gpm)	14
Oil inlet temperature	107
Winding maximum temperature	259

p is the copper electrical resistivity,

k is the copper thermal conductivity, and

z is the length of turn measured to the midpoint of the inner end turns.

Since this temperature rise is the largest in the thermal circuit, and since it depends on the square of the armature current density, it is obvious that the configuration is sensitive to this variable.

Tradeoff studies involving the number of poles and number of disks have resulted in the output shown in Table 20, which is arranged to clarify the trend of machine parameters. The fixed radial machine dimensions for the 40 KVA machine (operating at the 1.5 p.u. overload point) were shown earlier in Table 18. The results for this machine application, shown in Table 20, have been selected for a maximum hot-spot temperature at 250° C and are presented for the machine with minimum active length. This was obtained by varying magnet axial length, armature axial dimension, and armature current density.

The conclusion drawn from Table 20 is that changing the number of poles over a wide range does not significantly affect machine size, although the minimum size occurs at slightly different number of poles, the exact number depending upon the number of disks chosen. Since the winding disk thickness decreases for increasing number of disks, a four-disk machine appears impractical for manufacturing reasons.

The commutating reactance fell below the desired value (0.283 ohm). The value of the commutating reactance depends on the machine connection in the two-disk case. For the present calculation, the machine was assumed to be connected so that each disk contains all six phases. This is the only practical method of connection for three-disk, six-phase machines. Similar tradeoff studies have been performed for machines of higher ratings and lower base speeds, such as those found in the F103 application. The fixed machine dimensions and parameters are listed in Table 21(a).

Using these constraints of temperature, reactance, and radius, three variables - magnet thickness, armature winding thickness, and armature current density - were stepped systematically, and the results were scanned for minimum-length machines. Similar calculations were performed for 20-, 24-, and 28-pole machines. The armatures of these machines are assumed to be cooled by radial conduction along the windings to the outer periphery, where the heat is transferred

TABLE 20

CHARACTERISTICS OF 40 KVA MACHINES WITH DIFFERENT NUMBERS OF POLES

Two-Disk Machines

Number of Poles	14	18	22	24
Active Length, inches	1.86	1.84	1.87	2.00
Magnet Thickness, inches	0.55	0.55	0.55	0.60
Armature Thickness, inches	0.275	0.282	0.277	0.311
Armature Current Density, K-amp/in.^{2}	16	17	18	18
Commutating Reactance, ohms	0.21	0.21	0.21	0.22

Three-Disk Machines

Number of Poles	14	18	24	28
Active Length, inches	1.90	1.80	1.75	1.78
Magnet Thickness, inches	0.35	0.30	0.30	0.30
Armature Thickness, inches	0.18	0.20	0.18	0.19
Armature Current Density, K-amp/in.^{2}	16.5	17	19	19
Commutating Reactance, ohms	0.19	0.21	0.17	0.17

TABLE 21(a)
LIMITING MACHINE DIMENSIONS FOR F103 APPLICATION

F103 core shaft radius	3.000 in.
Rotor support cylinder	0.125
Clearance between end turns and rotor support	0.150
Smallest inside radius end turns	3.275
Maximum machine radius	7.000
Thickness of support frame	0.100
Clearance for cooling tubes & connections	0.25
Maximum end-turn outside radius	6.65

TABLE 21(b)
VARIABLES HELD CONSTANT FOR ANALYSES

Magnetization	0.84 wb/m^2
Magnet pole arc	60 Elect. Deg.
Armature space factor	0.5
Rotor speed	6340 rpm
Axial air gap between stator and rotor	0.050 inch
Oil coolant inlet temperature	107° C
Oil flow rate	2.9 gpm

to oil as shown in Figure 28. The internal winding conduction rise is the dominant temperature rise, and is proportional to the square of the sum of the radial pole span and the inner end-turn length. Shortening the radial span by increasing the inner radius will decrease this internal temperature rise and allow operation at higher current densities. Calculations were made for several cases of shortened pole span to explore this effect.

The results of the calculations for the 90 KVA generator are shown in Table 22. The general conclusion drawn from these data is that the minimum active length is relatively constant over the range chosen. There is a slightly decreasing trend when the number of poles is increased. The trend is upward when the radial pole span is decreased (disk inside-radius increased). A similar but more pronounced trend would be expected if the pole span were shortened by decreasing the outside radius; but this includes a penalty for the decreasing average radius. A few trial runs for six-disk machines showed that there is a small length penalty for increasing the number of disks. Since the machine rating of the F103 application called for rather large ratings, nine-phase machines were designed. Nine-phase machines are best realized when the number of disks is a multiple of 3.

The results for the 120 KVA generators are shown in Tables 23 and 24. A definite advantage is shown for six-disk machines in Table 24. The six-disk machines are approximately one inch shorter than the three-disk machines. Again, as in the 90 KVA generators, there is a broad range in the number of poles and inside radii, where the machine size is relatively constant.

Tradeoffs regarding the magnet energy have not been made in detail since the magnet flux density influence can be assessed rather straightforwardly. A magnetic energy increase of 10 percent allows a 5 percent increase in flux density in the coils. That results in a 5 percent decrease in axial winding length and thus a 5 percent decrease in the number of magnets required to magnetize the air space. Thus the machine size basically changes in inverse proportion to the magnet energy level but at half that rate.

The results of the disk PM machine can briefly be summarized as follows. The machine size as a function of the number of disks, the axial magnet length, and current loading shows definite minima. The number of poles has very little effect on the overall machine size. The magnet energy has a much larger but monotonic influence on the machine size. The obtainable optimum disk machine

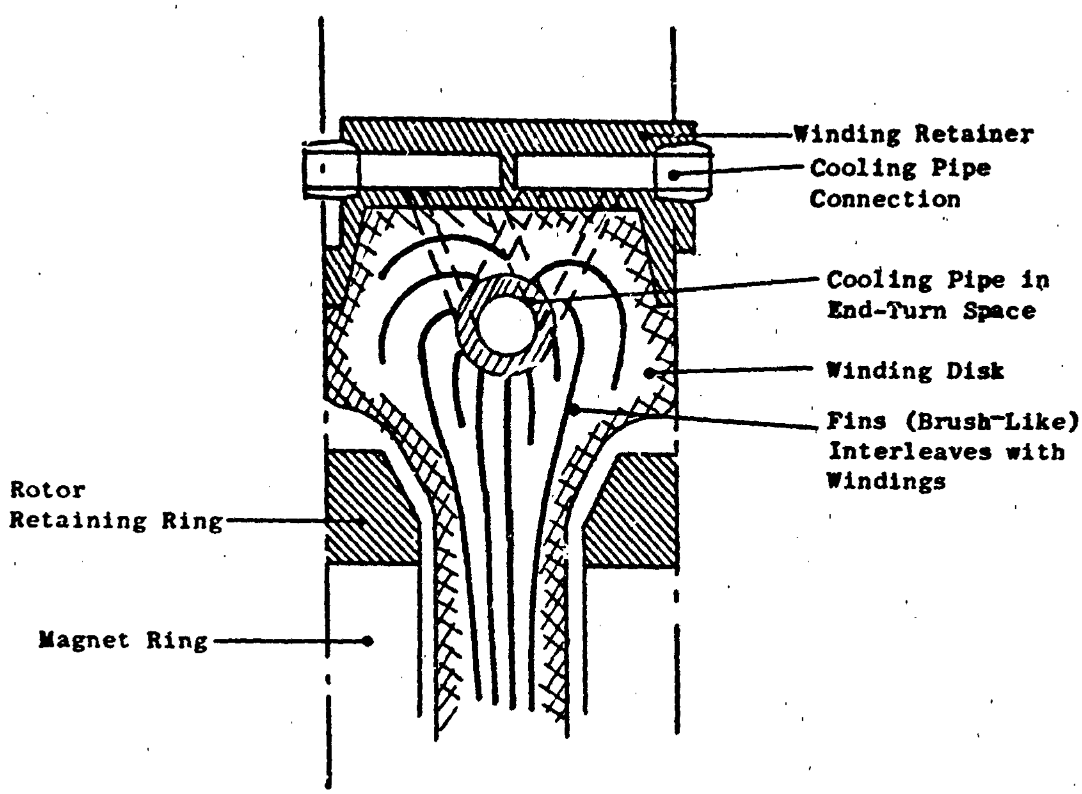


Figure 28. Cooling Concept of Winding Disk.

TABLE 22

SIZES FOR F103 DISK GENERATORS - 90 KVA NOMINAL, THREE-DISK MACHINES

5 Minutes Overload 145 KVA, 0.77 PF

Disk Outside Radius 5.9 Inches

Disk Inside Radius, inches		3.6			3.9			4.2	
Radial Pole Span, inches		2.3			2.0			1.7	
Number of Poles	20.0	24.0	28.0	20.0	24.0	28.0	20.0	24.0	28.0
Active Length, inches	2.48	2.42	2.38	2.52	2.41	2.33	2.66	2.48	2.40
Magnet Thickness, inches	0.48	0.44	0.42	0.52	0.48	0.44	0.58	0.52	0.49
Armature Thickness, inches	0.25	0.26	0.27	0.22	0.22	0.23	0.20	0.20	0.21
Armature Current Density $kA/in.^2$	11.8	12.0	12.4	12.8	13.2	13.6	13.7	14.5	15.0
Commutating Reactance, ohms	0.185	0.191	0.191	0.182	0.180	0.189	0.188	0.188	0.180

TABLE 23. SIZES FOR F103 DISK GENERATORS - 120 KVA NOMINAL, THREE-DISK MACHINES

5 Minutes Overload 232 KVA, 0.77 PF, Disk Outside Radius = 5.9 Inches.

Disk Inside Radius, in.	3.6		3.9	4.2
Radial Pole Span, in.	2.3		2.0	1.7
Number of Poles	20.0	24.0	20.0	20.0
Active Length, in.	5.18	6.38	5.26	6.06
Magnet Thickness, in.	1.12	1.48	1.20	1.50
Armature Thickness, in.	0.51	0.55	0.45	0.42
Armature Current Density, $kA/in.^2$	9.8	10.0	10.5	11.2
Commutating Reactance, ohms	0.137	0.141	0.138	0.141

TABLE 24. SIZES FOR F103 DISK GENERATORS - 120 KVA NOMINAL, SIX-DISK MACHINES

5 Minutes Overload 232 KVA, 0.77 PF, Disk Outside Radius = 5.9 Inches.

Disk Inside Radius, in.	3.6			3.9	4.2
Radial Pole Span, in.	2.3			2.0	1.7
Number of Poles	20.0	24.0	28.0	20.0	20.0
Active Length, in.	4.10	4.03	3.95	4.06	4.16
Magnet Thickness, in.	0.36	0.32	0.34	0.36	0.40
Armature Thickness, in.	0.22	0.23	0.22	0.21	0.19
Armature Current Density, $kA/in.^2$	11.4	11.5	12.2	12.1	13.2
Commutating Reactance, ohms	0.142	0.138	0.104	0.142	0.140

design, however, is mostly a function of the maximum hot-spot temperatures in the winding. In other words, a successful disk machine design depends very much on the cooling design concept. In the designs considered, the commutating reactance was not a design-influencing factor.

D. APPLICATION RECOMMENDATIONS

The previously discussed tradeoff studies have shown a few clear trends which are to be used for selecting optimum machines for specific ratings and applications. For the cylindrical machines, however, there has been no optimum way to design for lower weight. In order to get a small machine the variable design parameters have to be pushed to mechanical, electrical, and manufacturing limits. However, within the geometric and electric restrictions there do not appear to be any significant changes possible. Thus a good design will focus mostly on power loss, reliability, and winding temperature. The weight penalty for reduced losses appears to be relatively low for these applications.

For the disk-type machines there seem to be pronounced optimum points for weight and volume. In most instances these minima are quite broad and leave sufficient latitude for selecting the other parameters. The maximum winding temperature, however, has a rather significant influence on the machine design, as reflected by the cooling scheme that was used in the calculations. As a result, a vastly improved cooling scheme is necessary to obtain reasonable winding temperatures that are necessary for attaining a high machine MTBF. Thus, the disk machine needs more development work before it becomes practicable.

When electric power loss is examined (which does not include disk frictional losses) the disk machine is the more attractive concept because of the reduced stator losses (no iron losses) and reduced rotor eddy current losses. Both machines show relatively low additional losses from current time harmonics, in part because of the low armature reactance and armature reaction level in each machine. The disk machine has the added attraction of a rotor made entirely of low-conductivity material, which virtually eliminates eddy current losses in the rotor. The main eddy current losses in the cylindrical machine originate from the rotor slotting. A reduction of the slot opening to the smallest acceptable level - at the manufacturing stage - will help to reduce those losses to acceptable levels.

Following is a brief discussion of the differences in the rotor temperature characteristics of the radial and axial gap PM-type machines. Figure 29 shows the operating ranges and points for the magnets for a typical disk machine and a typical radial-gap machine. The difference in operating points becomes important when thermal magnetization loss is considered because the lower the operation point lies on the demagnetization curve the more severely do rare earth cobalt magnets suffer irreversible and, to some extent, reversible temperature-induced magnetization losses. Figures 30 and 31 (taken from a Raytheon Company magnet technical brochure) illustrate these susceptibilities. Thus, a radial-gap PM machine would tend to be somewhat less sensitive to high rotor temperatures than a disk machine, and should therefore be able to withstand somewhat higher rotor losses.

The sensitivity to thermal demagnetization becomes significant when the higher soakback temperature applicability of the concepts is considered, since there is thermal soakback after engine shutdown. The disk machine has a definite disadvantage under these conditions.

The cylindrical machine could occasionally experience electrical shorts in the windings, mainly because the windings are located in slots. Differences in thermal expansion coefficients will have to be compensated for by the insulation system. Thus it is mandatory that a suitable mechanical or electrical disconnect be developed for the cylindrical machine. The disk machine windings are much less prone to mechanical insulation breakthrough. Thus a simpler disconnect may suffice for this machine. The disk machine offers the possibility of active voltage regulation, but this comes at the expense of added complexity and weight. Regulating the voltage of the cylindrical machine would be impractical.

When ease of assembly is considered, the cylindrical machine looks like the more promising design, because its rotor and stator can be separately mounted and disassembled. However, excessive radial shaft movement (> 25 mil) could be a problem because of the machine air gap, which is on the order of 0.05 to 0.10 inch.

The disk machine's rotor and stator must be assembled together into the engine, but it is much less sensitive to radial shaft movement.

The considerations discussed above will be used for selecting the optimum machines for selected engine applications.

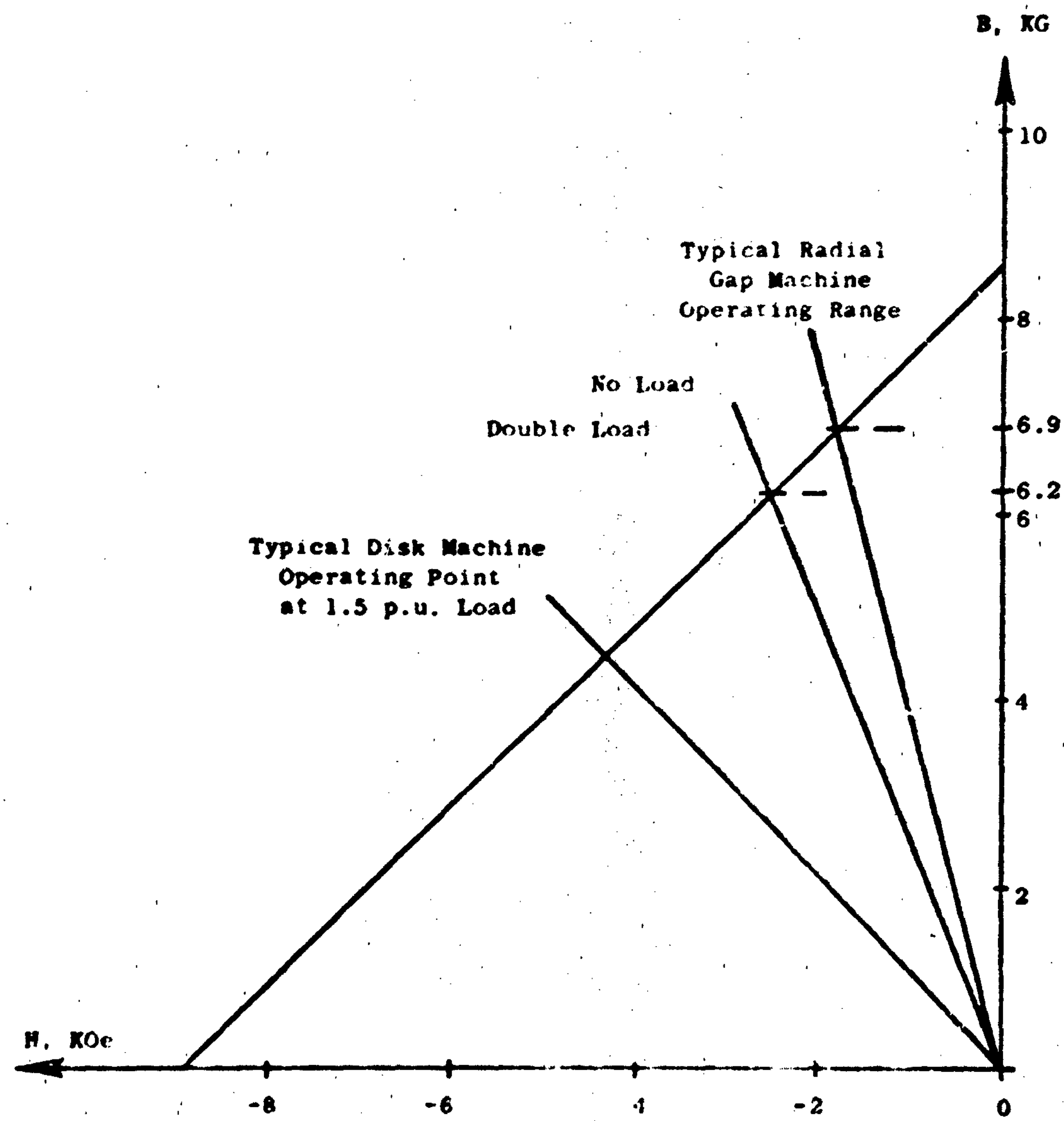


Figure 29. Magnet Operating Points.

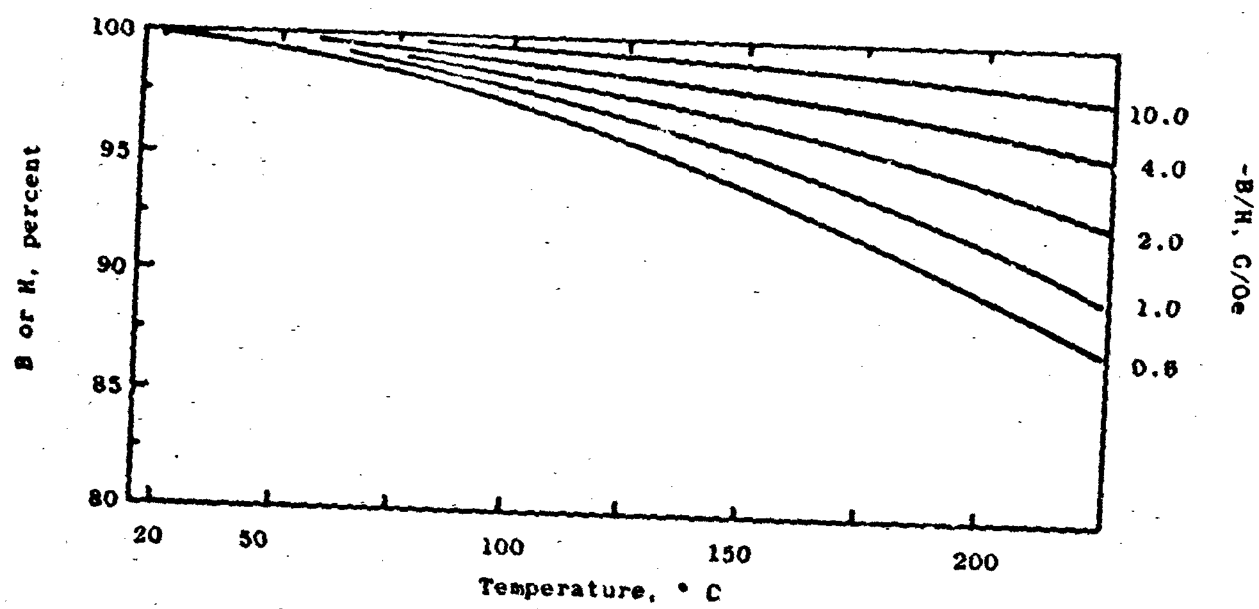
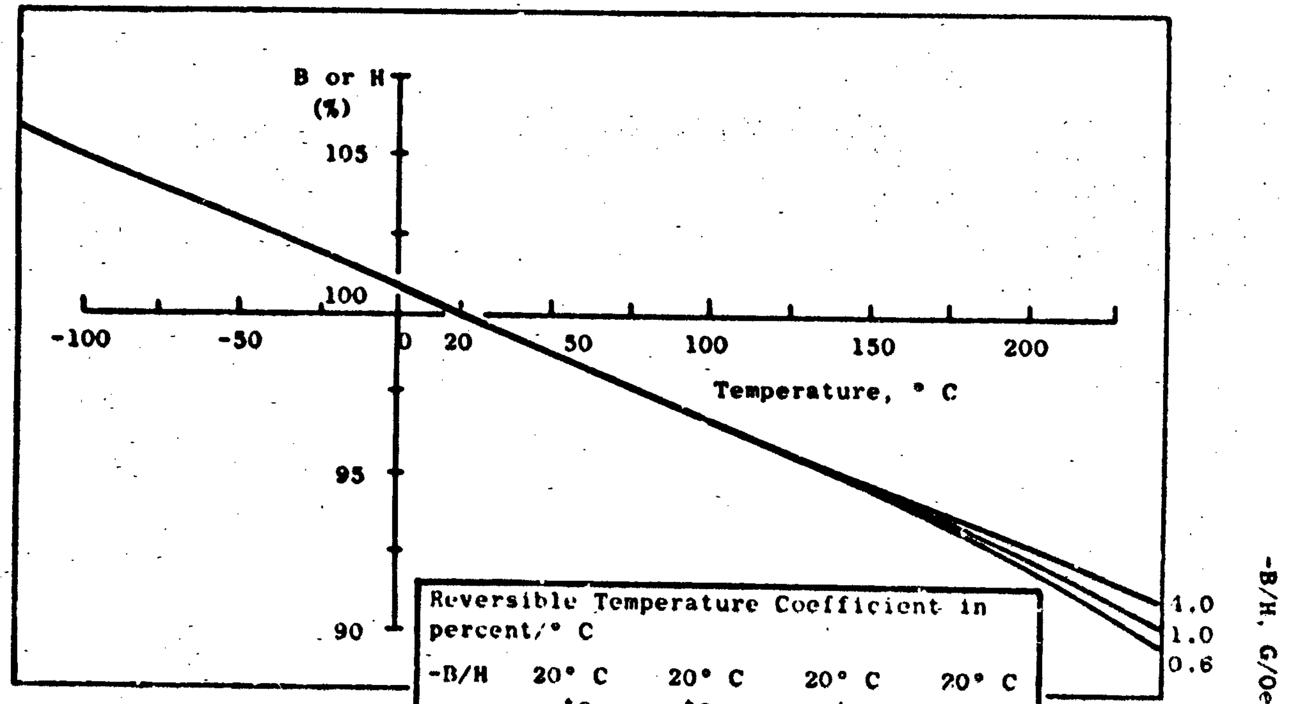


Figure 30. Irreversible Changes in Flux Density, B, and Demagnetizing Field, H, Measured at Room Temperature After Elevated Temperature Exposure.



Reversible Temperature Coefficient in percent/° C

-B/H	20° C to -100° C	20° C to 150° C	20° C to 200° C	20° C to 250° C
0.2	0.040	0.040	0.047	0.055
0.4	0.040	0.040	0.044	0.049
0.6	0.040	0.040	0.042	0.046
1.0	0.040	0.040	0.041	0.042
4.0	0.040	0.040	0.040	0.040

Above 20° C Values are Negative
Below 20° C Values are Positive
Guaranteed Absolute Values are no more than 15% Higher

Figure 31. Reversible Changes in B and H Measured at Temperature.

SECTION VI

GENERATOR/ENGINE INTEGRATION STUDY RESULTS

The conceptual generator/engine integration study successfully demonstrates the technical feasibility of the IEG/S design approach to electric secondary power extraction. This feasibility is evidenced by the fact that most of the generator/starters investigated fit into the confines of existing turbofan engines, as shown on the layout drawings in Addendum A, Drawings.

The conceptual layout studies provide the information necessary to define the problems associated with the generator/engine design integration and provide a base from which to evaluate the various designs realistically.

The main objectives of the integration study were: define the generator location, define its interfaces, explore the design of disconnect devices, and evaluate the feasibility of the IEG/S with respect to reliability, integration severity, safety, and cost.

The operating functions (generation, starting), the environmental conditions, and the accessibility determine the possible location of the IEG/S within the engine.

The optimum location for an IEG/S within a turbofan engine is the space between the fan and compressor, called the forward sump area (Figure 32). This location combines the advantages of cool environment, relatively large space (especially in high bypass turbofan engines), adequate accessibility, good structural support, and access for power transfer from and to the high-pressure turbine shaft, a capability which is essential for starting a dual-spool engine. Although a generator (Power Level I) can be driven by the low-pressure (LP) shaft, the disadvantages of wide speed range and low speed result in large, heavy generators. Also a split arrangement with two generators was investigated whereby one PM machine is driven off the HP shaft and operates as a starter/generator and the other one is driven by the LP shaft and operates as a generator only.

The above arrangement was found unattractive in terms of weight and mechanical complexity and was deleted from the study.

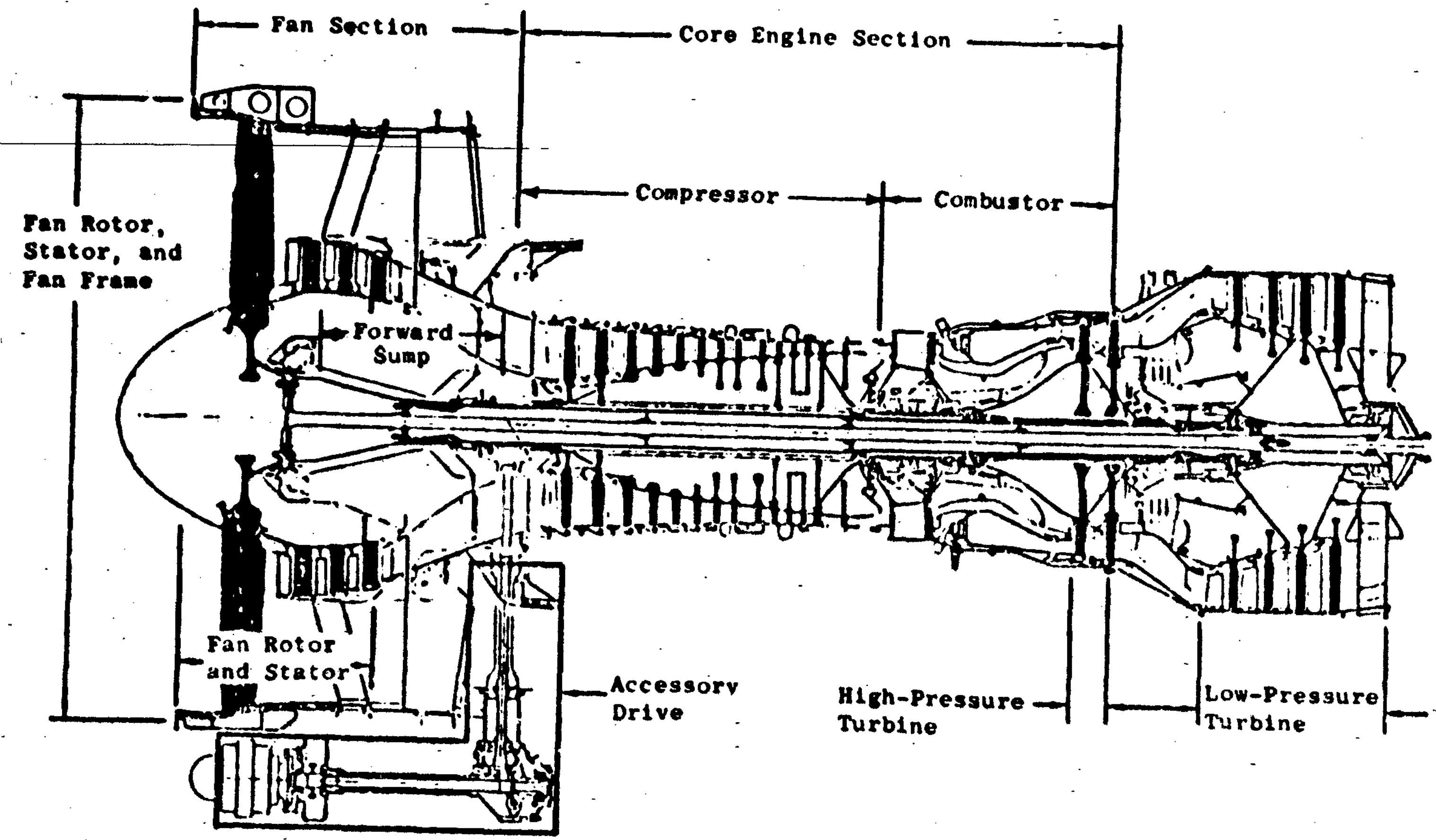


Figure 32. Typical High-Bypass Turbofan Engine.

A. ELECTRICAL POWER LEVELS CONSIDERED

1. Introduction

The specification requirements define three categories of electrical power to be considered for each of the three engine classifications. These categories were defined in Section IV-B, Electrical System Load Requirements.

The KVA ratings which were selected to correspond with the power level specifications for each of the selected engines are shown in Table 25. A detailed discussion and presentation of the data used to establish the selection of KVA ratings are found in Section IV-B of this report.

2. Summary of Interface and Power Level Consideration

The IEG/S integration studies resulted in layouts for each power level and PM machine type shown in Table 25 for each of the engines. These layout drawings are contained in Addendum A of this report. Table 26 summarizes the results of these integration studies and classifies the engine modifications on the engines. Combinations required marked by an "X" represent a moderate impact on engine modification.

The greatest potential payoff for an IEG/S exists for Power Levels III and IIIA, where the maximum usable electric power is provided. Use of the IEG for Power Level I does not offer any advantage since no other part of the system is simplified or eliminated. Power Level II offers advantages by eliminating the need for a separate engine starting system. Power Levels IIIA and III offer the greatest potential by eliminating the mechanical accessory drive system and engine-mounted accessories, therefore simplifying the secondary power system at the engine level, and at the aircraft level as well. More flexibility in the placement of electrically driven engine accessories is possible. The fuel and lubrication pumps, for instance, would be powered by variable-speed electric motors. Elimination of the engine gearbox generally reduces engine frontal area and therefore aerodynamic drag, a reduction which is critical in high speed aircraft.

The advantages inherent in the IEG/S concept appear to be at their maximum in aircraft on which the secondary power system is entirely electric. The full advantages of the IEG/S concept must be evaluated at the aircraft level. Weight savings, reliability improvement, and life cycle costs must be compared at the aircraft level to evaluate the total potential of the IEG/S concept.

TABLE 25
SELECTED KVA RATINGS

Engine	Power Level (KVA) I	II	IIIA	III
TF34 (RPV)	30/40	60	60/75	120
F404 Low Bypass	60/75	90	200	800
F103 High Bypass	75/90	120	300	1200

TABLE 26
POWER LEVEL/ENGINE/GENERATOR - MATRIX

Power Level			KVA Ratings F404	F103	TF34
I	KVA		60/75	75/90	30/40
		Cyl.	X*	X	X
		Disk	X*	X	X
II	KVA		90	120	60
		Cyl.	X*	X	X
		Disk	X**	X	X
IIIA	KVA		200	300	75
		Cyl.	X**	X	X
		Disk	X**	X	X
III	KVA		800	1200	120
		Cyl.	No-Go	No-Go	X
		Disk	No-Go	No-Go	X

X = Integration possible with only moderate engine modification

* = Amount of engine extension: 2.5 inches, major modification

** = Amount of engine extension: 3.6 inches, unacceptable

3. Machine Design Tradeoffs

Table 27 lists the limiting PM machine dimensions for the various locations within the three engines. Using the results of the tradeoff studies discussed in Section V as guidelines, the optimum cylindrical and disk machines for Levels I and II were selected from computer-aided designs for Location I of the TF34 engine. It was found that the total radial space of 1.2 inches for the electrical machines to be mounted within this location prevents the application of a disk-type machine, which needs more space radially than the cylindrical machine. Table 28 lists the main characteristic data for cylindrical PM generators with Level I and II ratings for the TF34, Location I.* The maximum dimensions of the electromagnetics package are the Punching Diameter and the Maximum Length (CL), which includes a 0.4 inch extension clearance past the rotor. The problem of this location is evident: it results in rather long machines (7.3 and 9.65 inches respectively).

Table 29 compares the basic characteristics for TF34, Location II* generators. As expected, the disk machines are significantly shorter than the cylindrical machines but larger in diameter. How much this benefits the engine design has to be established. The small rating produces a disk machine smaller and lighter than the cylindrical machine. In the larger rating both machines weigh about the same and need similar volumes but they assume different shapes. The benefit of the disk machines is their rather low commutating reactances, which will be beneficial to the cycloconverter operation with respect to efficiency and power capability. If a disk machine is selected, the electrical specifications should be changed to reflect the lower commutating reactance and its effects on the voltage required. The current densities of the disk machines have been allowed to be higher because of the absences of iron losses. As mentioned before, however, a more effective cooling scheme has yet to be developed for the disk machines.

*Location I is inside the forward end of the compressor; Location II is in the forward sump.

TABLE 27. LIMITING PERMANENT MAGNET DIMENSIONS.

Engine	Preliminary Envelope Drawing No. (Ref.)	System Power Level/KVA	Dimensions Min DS, in.	Max DT, in.	Max HI, in.	Comment
TF34	4013186-753	I/40	3.70	9.10	2.60	DT could be increased
		II/60	3.70	9.10	2.60	to 10.50 inches
	4013186-779	I/40	4.20	6.60	Open	HI per Dwg. = 3.4 inches
		II/60	4.20	6.60	Open	
	4013186-855	III	3.10	9.7	2.6	HI could be increased to 3.6 inches
F103 (CF6-50)	4013186-752	I/90	9.50	17.8	3.8	
		II/120	9.50	17.8	3.8	
	4013186-785	I/90	7.50	17.8	3.8	HI can be increased
		II/120	7.50	17.8	3.8	
	4013186-847	III	6.8	16.1	5.0	
	4013186-875 Split Power System	III	6.8 8.2	15.3 17.8	3.5 4.0	HPT starter/gen. LPT generator
F404	4013186-836	I/75	2.95	12.5	2.6	DT could be increased
		II/90	2.95	12.5	2.6	to 14.0 inches
	4013186-848	III	2.95	12.5	3.6	DT could be increased to 14.0 inches

DS = Shaft diameter

DT = Stator outside diameter

HI = Stack length

TABLE 28
CYLINDRICAL PERMANENT MAGNET MACHINES
FOR TF34 APPLICATION - LOCATION I

Rating, KVA	KVA	30/40	60
Base Speed, rpm	-	9380	9380
Base Frequency, Hz	F	1257	1407
Poles	-	16	18
Number of Phases	-	6	9
Stack Length, in.	BI	6.01	8.456
Punching Diameter, in.	OBS	6.600	6.600
Inside Rotor Diameter, in.	IBR	4.40	4.400
Length Over End Turns, in.	OL	6.90	9.222
Maximum Length, in.	CL	7.30	9.65
Rotor Diameter, in.	OBR	5.640	5.486
Magnet Height, in.	hm	0.465	0.452
Electrical Losses, kW	PT	4.5	8.6
Air-Gap Flux Density, KL/in.^{2}	B_{mg}	36	32
Commutating Reactance, R	X_c	0.253	0.223
Volume, in.^{3}	-	236	316

TABLE 29
COMPARISON OF DISK AND RADIAL MACHINES
FOR TF34 APPLICATION, LOCATION II, 9380 RPM

Rating, KVA	**30/40**	**30/40**	**60**	**60**
Type	**Disk**	**Cylinder**	**Disk**	**Cylinder**
Poles	**22**	**16**	**18**	**18**
Frequency, Hz	**1720**	**1251**	**1407**	**1407**
Phases	**6**	**6**	**6**	**9**
Commutating Reactance, Ω	**0.106**	**0.25C**	**0.085**	**0.271**
Overall Length, in.	**1.77**	**3.50**	**2.75**	**3.64**
Maximum Clearance, in.	**1.97**	**3.90**	**2.95**	**4.04**
Shaft Diameter, in.	**3.70**	**3.70**	**3.70**	**3.70**
Maximum Diameter, in.	**10.5**	**8.65**	**10.5**	**8.92**
Current Density, kA/in.^{2} @ 1 p.u. Load 0.95 PF	**11.8**	**8.5**	**11.1**	**7.9**
Volume, in.^{3}	**153**	**205**	**238**	**228**

Table 30 compares the radial and axial gap (cylindrical and disk) machines for Levels I and II and Locations I and II for the F103 engine. The same general trends can be observed: the disk machines are shorter than the cylindrical but are larger in diameter. At the higher ratings the volume of the disk machine also becomes larger than that of the cylindrical machine of the same rating.

That is just what happens in Location II. The minimum shaft diameter there is less restrictive, allowing a cylindrical machine with a higher power density. However, based upon observations made for both F103 and TF34 applications, the cooling concept and the maximum allowable hot-spot temperatures in the windings are the most severe limitations for a smaller disk machine. This underscores the need for further disk machine development. Cooling and winding temperature influence disk machine size, a fact which becomes clear when one realizes that a higher temperature and better cooling allow for thinner winding disks and thus less magnet strength. Therefore, a direct proportionality is expected between the machine size and the temperature.

Designing the machines for the F404 application is significantly more difficult than for the other two engines. A wider speed range and the same basic overspeed as the TF34 plus the space limitations made it difficult to obtain the desired design results. The 60/75 KVA system cylindrical generator just meets the 2.6-inch stack length requirements at the given diameter limitation. The 90 KVA machine could not meet it. Even varying the rotor diameter and number of poles did not help; this is demonstrated in Figure 33, which shows the stack length as a function of both these variables for a Level II F404 generator.

The disk machine did not have this problem, especially when the full diameter range was utilized. The results for the selections for both levels and machine types are shown in Table 31. The comments offered when comparing disk and cylindrical machines for the F103 application also apply when comparing how the two types of machines are suited for installation in the F404 engine. Considering shape and losses, the disk machine looks better. The cylindrical machine, however, does not require as much volume, as shown in Table 31. Again, the actual selection depends to a large degree upon the secondary benefits and the geometry limitation. Furthermore, it is believed that detail developments on the disk machine will reduce the volume, whereas higher-energy magnets will have very little effect on the cylindrical machines since all that is changed is

TABLE [illegible]

COMPARISON OF CYLINDRICAL AND DISK MACHINES
FOR THE F103 APPLICATION

Rating, KVA	75/90	75/90	75/90	75/90	120	120	120	120
Type*	C	D	C	D	C	D	C	D
Disks	-	3	-	3	-	3	-	3
Location	I	I	II	II	I	I	II	II
Poles	30	28	30	28	30	28	30	28
Frequency, Hz	1316	1228	1316	1228	1316	1228	1316	1228
Phases	9	9	9	9	9	9	9	9
Commutating Reactance, Ω	.106	.188	.158	.188	.109	.130	.115	.140
Overall Length, in.	4.43	2.01	3.74	2.08	4.60	3.01	4.64	3.26
Maximum Diameter, in.	13.94	17.8	12.60	17.8	14.52	17.8	12.61	17.8
Maximum Clearance, in.	4.83	2.21	4.14	2.28	5.00	3.21	5.04	3.46
Shaft Diameter, in.	9.50	9.50	7.50	7.50	9.5	9.5	7.5	7.5
Current Density at 1 p.u. Load, 0.95 PF - $kA/in.^2$	11.4	9.6	11.4	7.9	11.4	8.8	11.4	7.2
Volume, $in.^3$	676	500	466	517	761	749	579	811

*C = Cylindrical Machine

D = Disk Machine

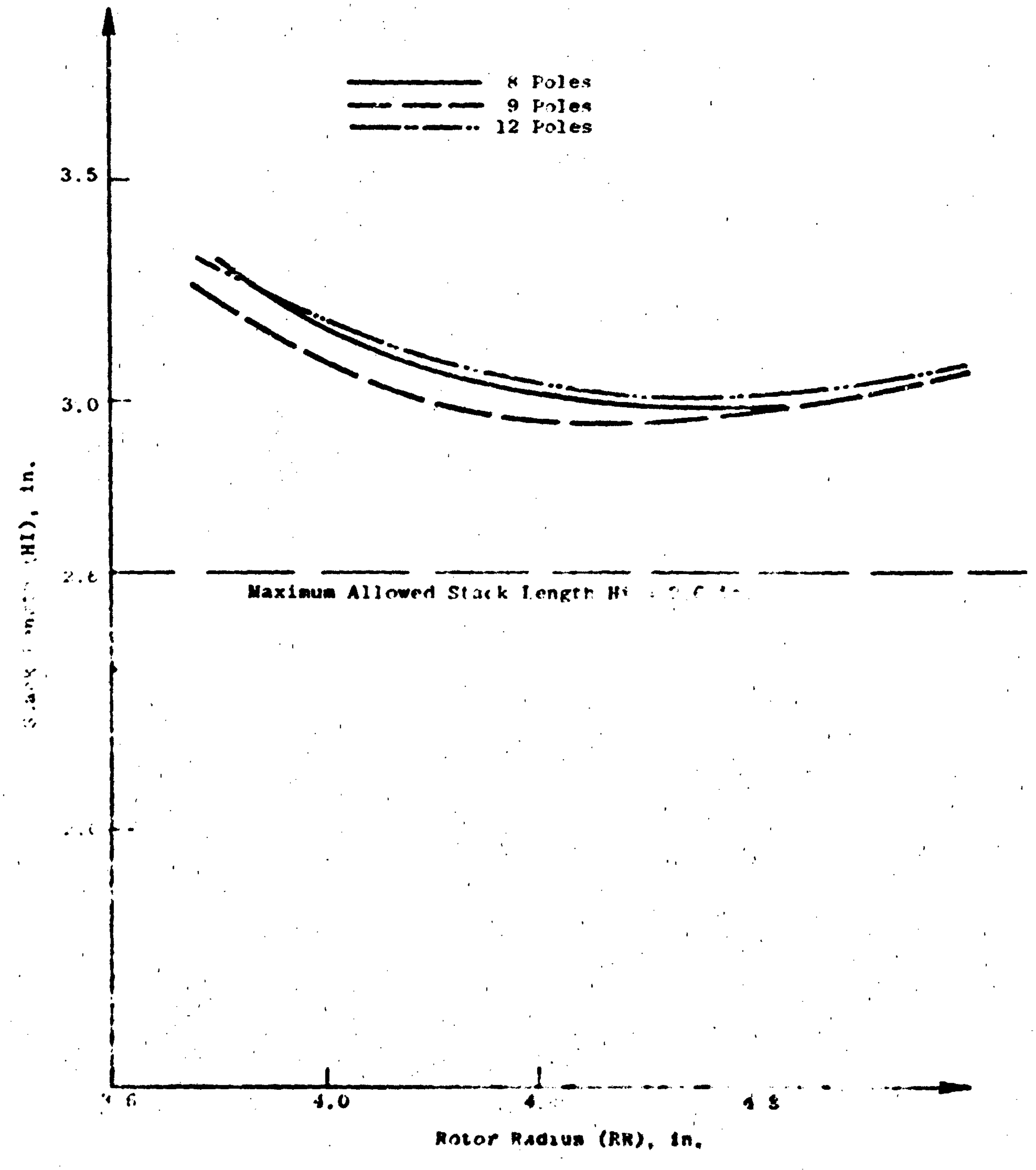


Figure 33. Stack Length (HI) versus Rotor Radius for Level II PM Generator for the F404 Application.

TABLE 31

COMPARISON OF DISK AND CYLINDRICAL PM MACHINES
FOR THE F404 APPLICATION

Rating, KVA	60/75	60/75	60/75	90	90	90
Type	C	D	D	C	D	D
Number of Disks	-	6	3	-	6	6
Poles	16	24	24	18	16	24
Frequency, Hz	1162	1743	1743	1307	1162	1743
Phases	9	9	9	9	9	9
Commutating Reactance, Ω	.231	.183	.226	.168	.160	.180
Overall Length, in.	3.85	3.30	2.19	4.16	4.91	3.34
Maximum Diameter, in.	9.24	12.5	14.0	9.75	12.5	14.0
Maximum Clearance, in.	4.05	3.50	2.39	4.36	5.11	3.54
Shaft Diameter, in.	3.30	2.95	4.45	3.60	2.95	4.45
Current Density at 1 p.u. Load, 0.95 PF - $kA/in.^2$	8.5	9.9	10.6	8.5	9.3	10.3
Volume, $in.^3$	258	405	337	511	603	545

the magnet volume required. Since the air-gap flux density and the current density stay mostly unaffected (for all those where Bg > 50 KL/in.^{2}), the change in magnet energy will affect only rotor stresses and rotor weight.

For the disk machine, however, a larger magnet energy means less active volume and thus more power per volume. A significant reduction in size would be expected to result.

As has been seen in Table 26, the Level III ratings for all three machines become very large, especially for the F404 and the F103. Thus, using the 3/4 power law (Equation 12) a quick projection has been made of the approximate machine sizes for the 800 KVA system configured for the F404 and the 1200 KVA system configured for the F103.

$$D^2L \cong \left[\frac{P_s}{rpm}\right]^{0.75} \tag{12}$$

rpm = Rotor Speed
D = Diameter
L = Length
P_s = Rating (KVA)

Table 32 shows the estimated maximum dimensions of the 800 KVA machine for the F404 engine and of the 1200 KVA machine for the F103 engine.

The estimates show that the 800 KVA for the F404 would result in generators too large for this engine, especially when one considers that the Level II generators for this engine were already marginal. The F103 generators may just be practical depending upon the details.

In reviewing the Level III generator sizes in more detail by engine type, the following observations can be made. The 60/75 KVA generator for the TF34 is similar to the 60 KVA Level II generator as far as the cylindrical version is concerned, since both will be designed for the double load point. Thus, no new cylindrical design for this rating is necessary. The 60/75 KVA disk machine, however, will be redesigned to provide some increase in cooling, since this machine is cooling sensitive. The 200 KVA machines for the F404 will be designed for both cylindrical- and disk-type machines. The Level III generator requirements for the F103 generator can also be met in a split generator arrangement; i.e., one generator mounted on the HP shaft, and one generator on the LP shaft. Since the LP shaft has 700 rpm as a minimum rated speed, any generator will

TABLE 32
ESTIMATED GENERATOR DIMENSIONS
FOR ALL ELECTRIC POWER EXTRACTION APPLICATIONS

Application	F404	F404	F103	F103
Rating, KVA	800	800	1200	1200
Machine Type	C	D	C	D
Max Diameter, in.	12.5	14.0	14.0	16.1
Max Length, in.	28.5	27.5	18.0	20.5

become quite large. Therefore, only the 300 KVA requirement will also be investigated for a split arrangement. The minimum HP generator is the one used for Level II (120 KVA). That leaves 180 KVA for the low-speed machine. Geometry and low speed do not allow the low speed generator to operate into a 400 Hz system. Since the additional load is basically of the electric motor type, which can operate at wild frequency and constant V/F, a wild frequency generator is assumed for the low speed machine, which has a possible frequency range at base speed of 280 to 385 Hz maximum. That, of course, allows utilization of 400 Hz motors at the high frequency end.

Tables 33 through 35 show the specifications for the Level III generators to be studied. The 120 KVA machine for the split generator arrangement of the F103 as well as the 60/75 KVA cylindrical machine for the TF34 are not included.

Selection of the machines from the design matrix was generally done with respect to an optimum combination of minimum stack length or overall length and machine volume. However, if a shorter stack length than the one given is desired, this often can be accommodated at the expense of the volume and weight of the machine. The Level III generators, more so than Level I and II machines, are open to more optimization because of their size.

The characterizing data for cylindrical machines for the VSCF application selected are listed in Table 36. While all the machines meet or stay below the diameter restrictions, only the 150 KVA and 300 KVA machines meet the stack length requirements. The results for the disk machines are listed in Table 37. In this case none of the machines listed meet the desired axial length (stack length) requirements of Table 34. However, these machines do not compare well with the overall length dimensions of the Level III cylindrical generators either. One of the reasons for the larger volume and length of the disk machines is the naturally lower power density in the air gap because of the larger magnetic air gap and the resultant lower flux density (about one-half of that found in cylindrical machines). The geometric advantage which the smaller-rating disk machines gain from superior volume utilization disappears for the large ratings.

The characterizing data for the wild frequency generators for the split generator application are listed in Tables 38 and 39. Not all machines meet the stack length requirements, a fact which is expected for full power at 700 rpm (lower than most 60 Hz machines). Other than that, it is interesting to note that the maximum possible number of poles does not yield the smallest volume

TABLE 33
SPECIFICATIONS FOR LEVEL III HIGH SPEED
(VSCF) CYLINDRICAL-TYPE GENERATORS

Engine	TF34	F404	F103	F103	F103
Rating, KVA	120	200	150	300	1200
Base Speed, rpm	9380	8715	5262	5262	5262
Overspeed, rpm	21,472	20,508	11,990	11,990	11,990
3 ϕ Load Current at 2 p.u., amps	626	1072	804	1608	6432
Voltage, V	145	145	145	145	145
Power Factor	0.69	0.68	0.68	0.68	0.68
Comm. Reactance, Ω	0.141	0.113	0.151	0.0734	0.0189
Shaft Diameter, in.	3.1	2.95	6.8	6.8	6.8
Max Diameter, in.	9.7	12.5	15.8	16.1	16.1
Poles (min)	16	16	24	24	24
No. of Phases	9	12	12	12	12
Desired Stack Length, in.	2.6	3.6	3.5	5.0	5.0

TABLE 34
SPECIFICATIONS FOR LEVEL III HIGH SPEED (VSCF)
DISK-TYPE GENERATORS

Engine	TF34	TF34	F404	F103	F103	F103
Rating, KVA	60/75	120	200	150	300	1200
Base Speed, rpm	9380	9380	8715	5262	5262	5262
Overspeed, rpm	21,472	21,472	20,508	11,990	11,990	11,990
3 Φ Load Current at 1.5 p.u. Load, A	248	497	840	630	1260	5040
Voltage, V	155	155	155	155	155	155
Power Factor	0.77	0.77	0.76	0.76	0.76	0.76
Comm. Reactance, Ω	0.226	0.141	0.113	0.151	0.0734	0.0189
Shaft Diameter, in.	3.1	3.1	2.95	6.8	6.8	6.8
Max Diameter, in.	9.7	9.7	14.0	15.8	16.8	16.1
Poles (min)	16	16	16	24	24	24
No. of Phases	9	9	12	12	12	12

TABLE 35
SPECIFICATIONS FOR LEVEL III WILD
FREQUENCY GENERATORS, BOTH TYPES

Rating, KVA	150	150	180	180
Type	C	D	C	D
Rated Speed, rpm	700	700	700	700
Max Speed, rpm	3432	3432	3432	3432
Overspeed, rpm	4187	4187	4187	4187
3 Φ Load Current	804	630	965	756
at Load, A	2.0	1.5	2.0	1.5
Voltage, V	145	155	145	155
Power Factor	0.68	0.76	0.68	0.76
Comm. Reactance, Ω	0.151	0.151	0.126	0.126
Shaft Diameter, in.	8.2	8.2	8.2	8.2
Max Diameter, in.	17.8	17.8	17.8	17.8
Stack Length (Described), in.	4.0	4.0	4.0	4.0
Poles (max)	66	56	66	56
Min Frequency, Hz	385	327	385	327
Max Frequency, Hz	1888	1602	1888	1602
Phases	12	12	12	12

for the machine. Note, too, the rather large frequency range of these machines, which obviously goes along with a similar voltage range; this needs to be kept in mind for the system separation.

TABLE 36
RESULTS OF THE TRADEOFF STUDY
FOR HIGH SPEED CYLINDRICAL GENERATORS
FOR LEVEL III POWER REQUIREMENTS

Application	TF34	F404	F103	F103	F103
Rating, KVA	120	200	150	300	1200
Poles	18	24	32	32	32
Frequency, Hz	1407	1743	1403	1403	1403
Phases	9	12	12	12	12
Commutating Reactance, Ω	0.124	0.076	0.127	0.069	0.016
Stack Length, in.	4.91	5.86	2.59	4.34	16.7
Overall Length, in.	6.03	6.64	3.64	5.44	17.8
Maximum Diameter, in.	9.21	9.28	15.54	15.99	16.0
Maximum Clearance, in.	6.43	7.04	4.09	5.84	18.2
Shaft Diameter, in.	4.54	3.78	6.87	6.82	6.95
Current Density at 1 p.u. Load, 0.95 PF - kA/in.^{2}	8.5	8.3	8.3	8.3	8.3
Volume, in.^{3}	402	449	609	1092	3577

Again, the disk machines require much more space than the cylindrical machines, mainly because of the relatively inefficient way of cooling large disk machines as pointed out before, but also because of the disk machine's inherently lower current density level. On the other hand, the actual sizes for the very large machine ratings turned out to be not quite as bad as were originally forecasted.

Summarizing this part of the study, it can be said that the original concerns about the influence of the restricted geometry and speed range have been verified; that is, integrated machines tend to be larger and thus heavier than their counterparts on the auxiliary gearboxes. For certain applications, however, machines resulted which look very reasonable and practical, including their promise for meeting the requirement for high MTBF.

The study has covered preliminary design projections for three different rating classes for three engine applications and two generator types. Because of the level of analysis employed, no clearcut winner has been found. The cylindrical machines have been found to be more compact in most cases because of their higher air-gap power density and superior stator cooling system. Since

TABLE 37

LEVEL III DISK-TYPE
GENERATORS - SUMMARY

Engine Application	TF34	TF34	F404	F404	F103	F103	F103	F103
Nominal Rating, KVA	60/75	120	200	200	120	150	300	1200
5-Minute Rating, KVA	115	231	391	391	231	292	586	2344
Phases, Disks	9,6	9,9	12,8	12,8	9,6	12,8	12,12	12,40
Poles	20	20	20	16	30	30	28	30
Base Speed	9380	9380	8715	8715	5262	5262	5262	5262
Base Frequency	1560	1560	1452	1162	1315	1315	1228	1315
Magnet Outer Radius	3.86	3.86	5.00	4.48	6.61	6.61	6.61	6.61
Magnet Inner Radius	2.15	2.15	3.00	2.17	3.52	3.52	3.52	3.52
Machine Outer Radius	4.85	4.85	7.00	6.25	7.9	7.9	7.9	7.9
Magnet Axial Thickness	0.32	0.41	0.44	0.76	0.28	0.26	0.36	0.48
Armature Axial Thickness	0.16	0.24	0.25	0.39	0.25	0.22	0.30	0.36
Total Magnetic Length	3.66	7.02	6.55	10.27	3.94	4.86	9.21	37.63
Armature Current Density @ 1.5 p.u. Load - amp/in.^{2}	18.7	18.0	15.4	14.0	10.6	11.0	10.9	10.6
Commutating Reactance	0.22	0.22	0.117	0.11	0.107	0.11	0.06	0.015
Volume, in.^{3}	270	519	1008	1260	772	952	1806	7372
Power Density, KVA/in.^{3}	0.43	0.44	0.39	0.31	0.30	0.31	0.32	0.32
Oil Flow GPM	8	15	15	20	4.4	8.7	21	52
Armature Current Density @ 1 p.u. Load - Amp/in.^{2}	12.4	12.0	10.1	9.2	7.0	7.2	7.1	6.9

TABLE 38
RESULTS OF A TRADEOFF STUDY
FOR WILD FREQUENCY CYLINDRICAL GENERATORS
FOR THE F103 APPLICATION

Rating, KVA	150	180
Poles	54	48
Frequency - Max, Hz	1544	1373
- Min, Hz	315	280
Phases	3	3
Comm. Reactance, Ω	0.146	0.125
Stack Length, in.	9.45	11.49
Overall Length, in.	10.41	12.30
Max Clearance, in.	10.81	12.70
Max Diameter, in.	17.8	17.72
Shaft Diameter, in.	10.54	11.49
Current Density at 1 p.u. Load, 0.95 - $kA/in.^2$	8.3	8.3
Volume, $in.^3$	2531	3037

TABLE 39
RESULTS OF TRADEOFF STUDY FOR WILD FREQUENCY
DISK GENERATORS FOR THE F103 APPLICATION

Nominal Rating, KVA	150	180
5-Minute Rating, KVA	292	352
Phases, Disks	12,28	12,36
Poles	36	36
Base Speed, rpm	700	700
Base Frequency, Hz	268	268
Magnet Inner Radius, in.	5.7	5.7
Magnet Outer Radius, in.	7.8	7.8
Machine Outer Radius, in.	8.9	8.9
Magnet Axial Thickness, in.	0.40	0.36
Armature Axial Thickness, in.	0.23	0.20
Total Magnetic Length, in.	20.6	24.1
Armature Current Density @ 1.5 p.u. Load, $kA/in.^2$	13.4	14.0
Commutating Reactance, ohms	0.121	0.092
Volume, $in.^3$	5126	5997
Power Density, $KVA/in.^3$	0.057	0.059
Oil Flow, gpm	36.4	54.6
Armature Current Density @1 p.u. Load, $kA/in.^2$	8.9	9.3

the magnets in that type of machine can withstand higher temperatures, they are better for those applications where soakback temperatures occur. The rotor eddy current losses are a disadvantage, but as long as the resultant temperatures do not interfere with the rotor mechanics, the loss problem may be compensated for by the capability of withstanding high soakback temperatures (up to 250° C). Furthermore, the loss analysis has shown that by properly designing the slot opening and air gap, one can significantly reduce the losses.

On the other side, the disk machines do not have any electric rotor losses to speak of. However, because of the cooling concept presently employed, disk machines having an inside-to-outside rotor radius ratio of less than 0.8 would be larger than the comparable cylindrical machines. Also, in the case of high soakback temperatures the disk machines are at a disadvantage because maximum temperatures should be kept at 250° C or lower, the exact maximum depending upon the magnet length.

B. SOLID STATE CONVERSION DESIGN

The solid-state conversion design on this program was limited to establishing and estimating the size, weight, and cooling requirements of a cycloconverter with power protection circuits that would be appropriate for each of the systems listed in Table 25. In each case an SCR type was chosen to confirm that an appropriate power device is available. The results of this design analysis are shown in Table 40.

C. DISCONNECT DESIGN

Because a permanent magnet machine lacks excitation control there is no way to turn off the excitation when a fault occurs. Faults in the cycloconverter and at the output terminals of the converter can be disconnected within the converter. Faults in the high frequency cables between the input terminals of the cycloconverter and the generator, as well as faults within the generator, cannot be deenergized without special devices. The disconnect is especially required in a PM machine, where in the event of an internal machine fault or feeder fault, the electrical power output must be interrupted without affecting normal engine operation. Maximum short-circuit current levels for a short at the generator terminals can reach five p.u. for a symmetrical three-phase effort and up to nine p.u. for an unsymmetrical fault (that is, a line-to-neutral short), creating

TABLE 40
CYCLOCONVERTER CHARACTERISTICS

Power Level	System Ratings (KVA)	Machine Phases	Converter Efficiency (%) PF=1	Heat Rejection (Watts)	Cooling Air Requirements (lb/min) @ 40° C	Size ($in.^3$)	Weight (lb)	SCR Type
I	30/40	6	94	2,400	6.7	1,500	40	GE C148 Power Module
	60/75	6	94	4,500	13.5	2,174	58	GE C158 Chips In Power Module
	75/90	9	94	5,400	15.0	2,550	68	GE C158 Chips in Power Module
II	60	6	94	3,600	10.0	2,100	56	GE C158 Chips in Power Module
	90	9	94	5,400	15.0	2,810	75	GE C158 Chips in Power Module
	120	9	94.5	6,600	18.3	3,410	91	GE C186 WH T607 -- 18
IIIA	60/75	6	94	4,500	12.5	2,250	60	GE C158 Chips in Power Module
	200	9	95	10,000	27.8	4,000	125	WH T707 -- 26
	300	9	95	15,000	41.7	6,190	165	WH T707 -- 35
III	120	9	94	6,600	18.3	3,410	91	GE C186
	600	9	95.5	36,000	100	13,000	365	GE C448 WH T9GH -- 09
	1200	9	96	54,000	150	19,000	525	GE C648 WH T9GH -- 11

GE = GENERAL ELECTRIC PART TYPE
WH = WESTINGHOUSE PART TYPE

tremendous heating. In order to limit the time duration of these short-circuit currents, a means to interrupt, eliminate, or reduce the short-circuit currents must be included within the system. Three general protection concepts are considered to be capable of fulfilling this requirement.

- Interrupt input torque
- Increase air-gap length to decrease effective air-gap flux
- Interrupt (open) circuit

Initial work toward the safety disconnect was based on the understanding that a mechanical, resettable disconnect device is required.

The mechanical complexity of resettable disconnect devices and separate generator rotor support systems makes these devices unacceptable for engine integration from the standpoint of reliability. Therefore, an approach based on a circuit-interrupt (fuse neutral lead), nonresettable disconnect is being recommended for the selected disconnect concept. This concept also made it possible to simplify the generator/starter design by mounting the generator rotor rigidly onto the HP shaft.

The types of disconnect concepts investigated during this study are discussed below.

1. <u>In-Line Resettable Electromagnetic Disconnect</u>

This disconnect would be similar to the one described in AFAPL-TR-78-104, in which a solenoid coil pulls an armature plate toward it and allows a sear to move. Once the sear moves, the clutch plates are allowed to separate in the axial direction and the rotor is disconnected from the drive. The mechanical complexity of the disconnect device - which requires separate generator bearings and numerous mechanical parts - makes this device unacceptable for engine integration due to the need for a high-reliability device.

2. <u>Hydraulic-Actuated Resettable Disconnect Clutch</u>

Figure 34 shows the general arrangement of the hydraulic-actuated resettable coupling in the engaged position.

A central control unit monitors signals received from sensors transmitting electrical, thermal, and vibrational data. In the event of a potential failure, the solenoid valve receives a control-unit command to open the inlet port to the actuator cavity. Pressurized oil from the lube oil/cooling circuit energizes

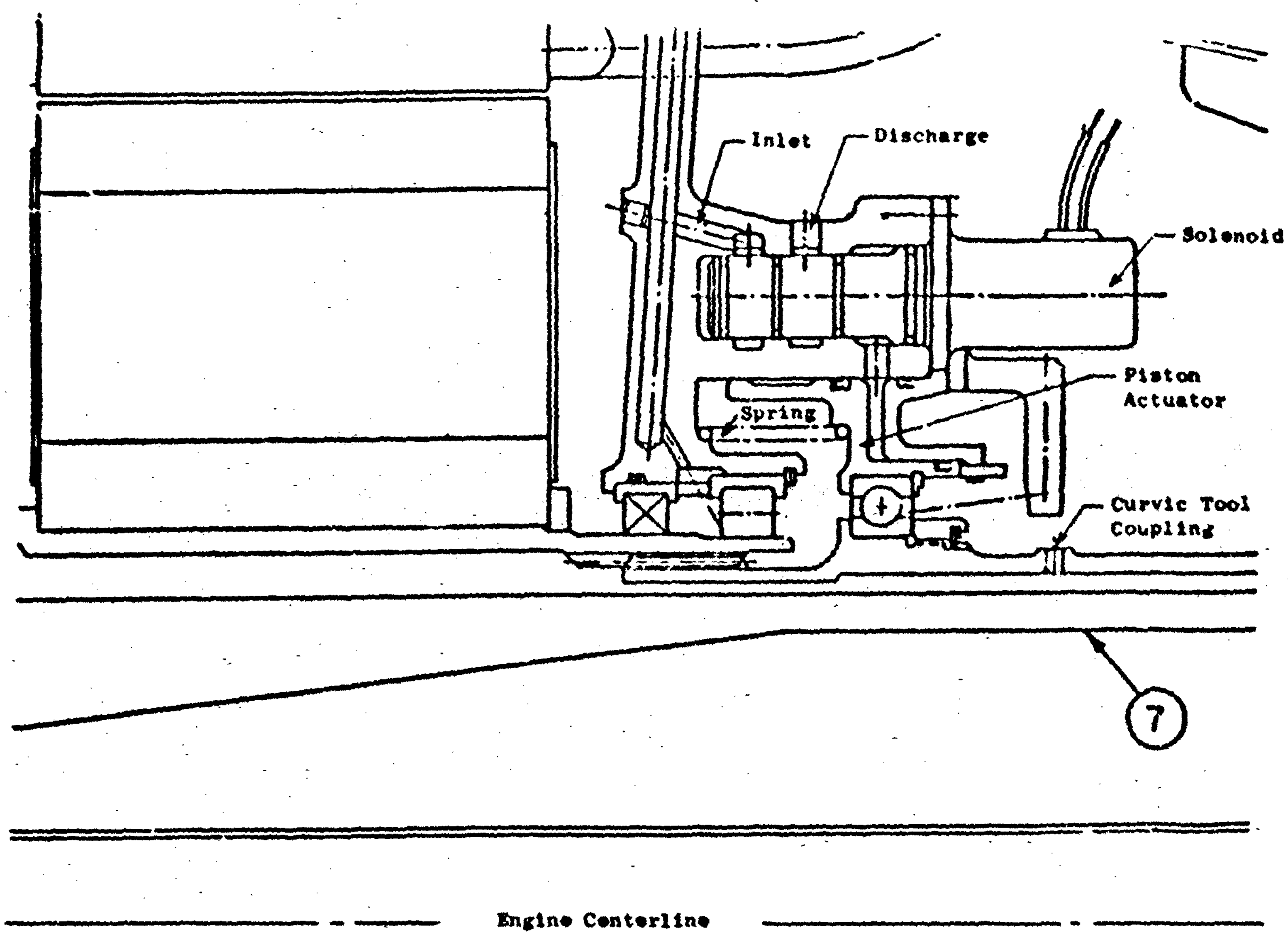


Figure 34. Hydraulic-Actuated Resettable Disconnect Clutch.

the actuator, moving it against a spring force to the left. The piston actuator, which is connected to a splined shaft through a ball bearing, disengages the curvic spline coupling, interrupting the power transfer from the HP shaft to the generator rotor. At this point the solenoid valve moves to a neutral position, blocking inlet and outlet ports of the actuator cavity to prevent a coupling reengagement even after a loss of system lube pressure.

Reengagement (resetting) of the coupling occurs only at rest after the solenoid receives a corresponding signal to open the discharge port. The spring load then returns the piston to the right and reengages the tooth coupling.

The disadvantage of this device lies in its mechanical complexity, with its additional set of generator bearings.

3. Tapered Rotor/Stator Configuration

Shown in Figure 35 is a cross-sectional view of the tapered rotor/stator configuration which is used in certain industrial applications. An axial shift of the rotor or stator reduces the voltage generated through the increase in air gap and reduction of effective stack length. Calculations have shown that for an axial rotor shift of 2 inches, for a machine with an outside diameter of 7.8 inches, and a stack length of 4.66 inches, the 3-phase short-circuit current would be reduced to 1.73 p.u. for a taper angle of 10 degrees. More axial shift, however, is required to reduce the short-circuit current level to thermally acceptable levels for continuous operation.

The following disadvantages eliminate this configuration from further consideration:

- Additional axial engine space is required.
- The size of the generator will increase as compared to a conventional machine: The maximum rotor diameter stays the same due to the mechanical stress limitations, while the reduced average rotor diameter necessitates an increase in stack length.
- The high manufacturing cost of building such a high-speed tapered rotor/stator machine makes this configuration undesirable.
- The axial actuator mechanism, which would shift the stator, requires moving parts and reduces the total machine reliability.

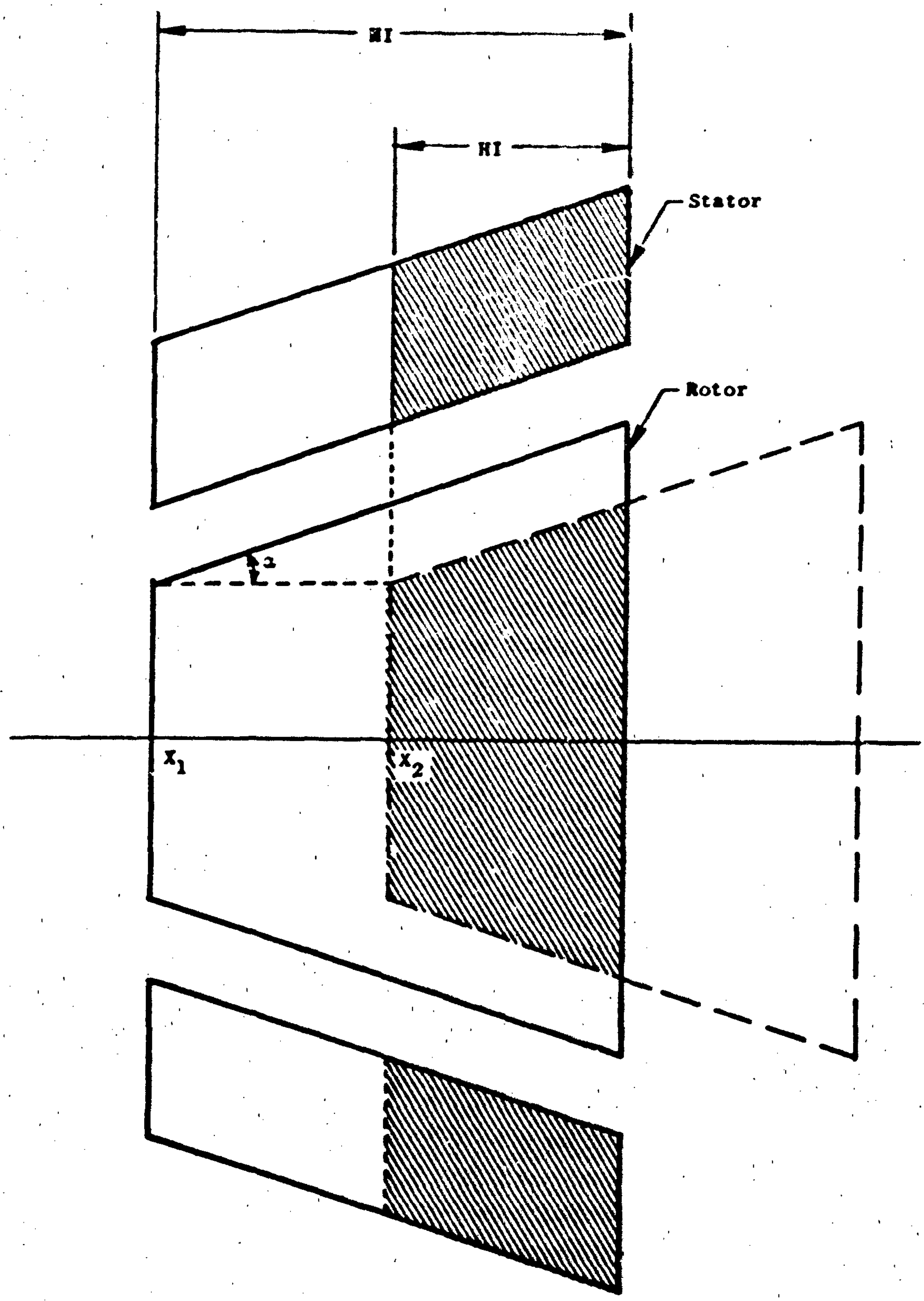


Figure 35. Tapered Rotor/Stator.

4. Split Stator Approach

For this approach, the stator core is fabricated in two half-circle sections which are hinged at one side and allowed to separate at the other (clam shell). The end-turns are arranged such that no end-turns cross the gap at which the stator core is to open.* Flexible lead connections and flexible cooling connections are required at the hinged side to allow movement of the stator core sections.

Calculations have shown that for a machine with an outside diameter of 7.8 inches, a stack length of 4.66 inches, and an air gap of 0.071 inch, an average air gap increase of 0.78 inch is required to reduce the 3-phase short-circuit current to a thermally acceptable level of 1 p.u.; that is, the machine has to be mounted in a space which can accommodate an increase in stator diameter to 9.36 inches in the actuated position.

The key points of this approach are as follows:

- Additional radial space is required.
- The basic machine size does not increase since the electromagnetics are not changed.
- Manufacturing is not significantly different from the methods used for conventional machines. The end-turn connections and cooling lines which cross the hinged core gap have to be arranged such that they can deform in case the stator is opened by the actuator.
- The actuator mechanism which would open the stator requires more parts and reduces the reliability of the total machine.

This concept is not practical due to the mechanical complexity involved to split the stator.

5. Fuse Neutral Leads

Shown in Figure 36 is the schematic diagram of a three-phase machine with fuses located in the neutral leads. This configuration is shown for an IEG/S design in Figure 37. The high-frequency line contactor in the converter will open in the event of fault detection, removing the power flow in the undamaged phase, but power flow in the damaged phase would still be present. Thus the fuse must be located in the neutral lead to open the circuit.

*This approach is utilized for commercial machines.

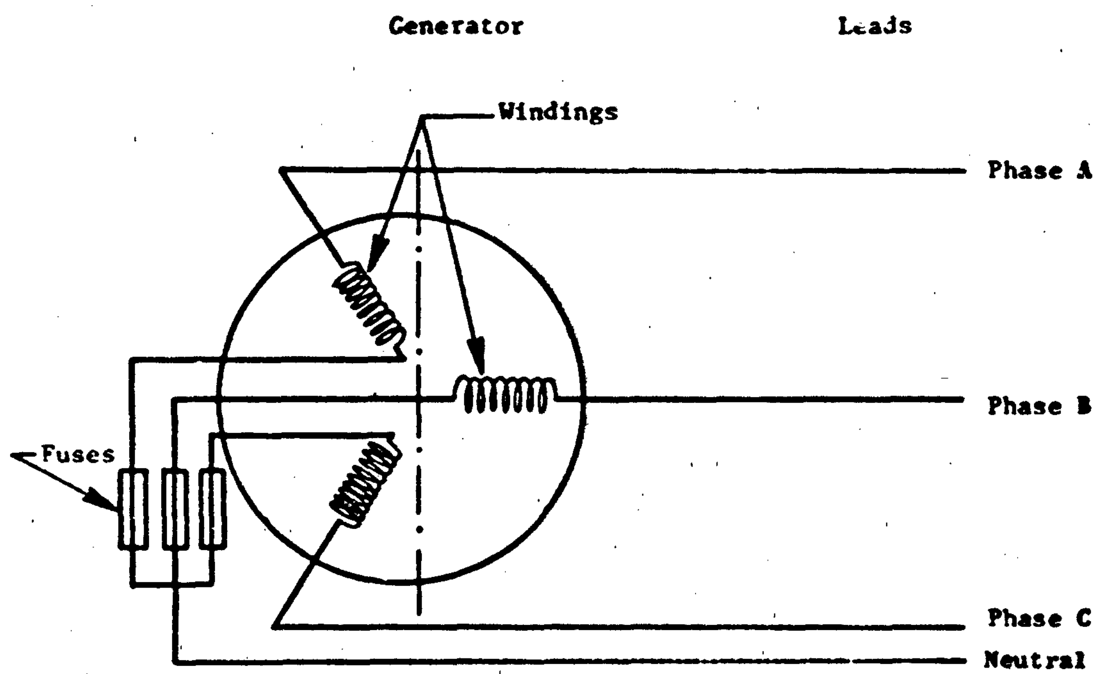


Figure 36. Fused Three-Phase Stator.

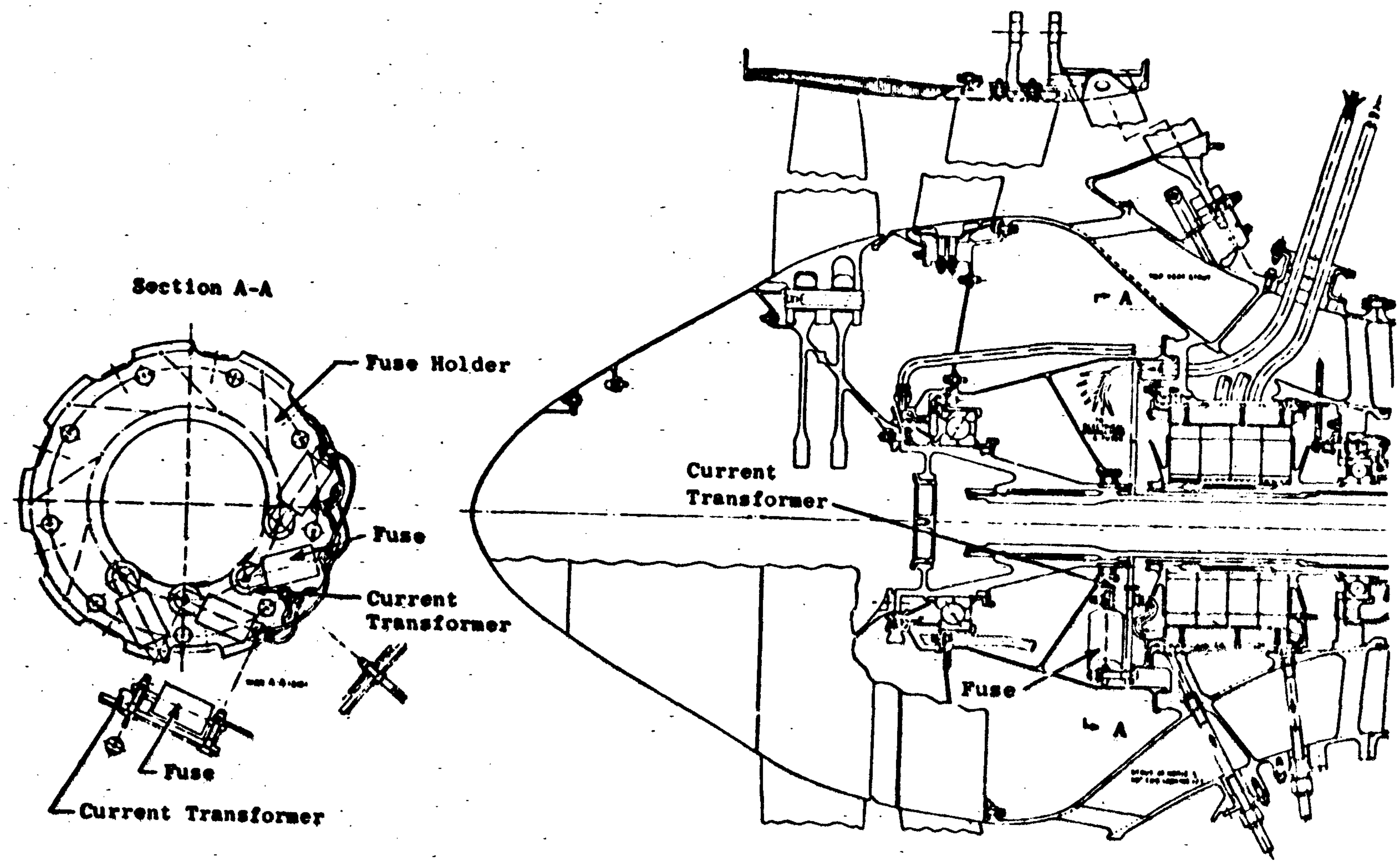


Figure 37. Fuse-Neutral-Lead Disconnect.

The location of the fuse should be as close to the stator windings as possible to minimize the possibility of cable faults between the stator winding and fuse location.

The fuse is a device which is temperature sensitive and which, within the normal engine operation ambient requirements, -55° C to 120° C, creates a significant challenge. The -55° C would be experienced during ground starting after the engine had cooled to ambient. If failure of the starter/generator were to occur during the cold ambient condition, the mission would be aborted. After the engine is running, the ambient temperature around the generator would soon be above 0° C, which provides more acceptable fuse operating ambient temperature of 0° C to 120° C.

Two modes of operation could be affected with this concept. The first would be by normal fuse operation. Currents above normal operation envelope requirements would be experienced and after time exposure the fusable link would open. The second mode of operation could be affected by detection of an undesirable operating condition, and the converter is programmed to short-out the generator winding to blow the fuses.

This concept provides protection for all short conditions except failures within the same phase. Generally, most faults in the winding are grounding faults at the stator slot entrance. Such faults arise from damaging the wire insulation, which can occur under the following conditions:

- during winding insertion
- operational vibration - wires flexing
- electromagnetic forces - wires flexing
- temperature cycling - wires flexing

The above condition can be minimized by adding stator end laminations which serves as a cushion to the end-turn wires.

Protection could be provided for winding faults within the same phase by the placement of another fuse within the phase winding (Figure 38). This concept is feasible but is considered to be impractical because it requires doubling the number of fuses used. In a nine-phase generator, 18 fuses would be required.

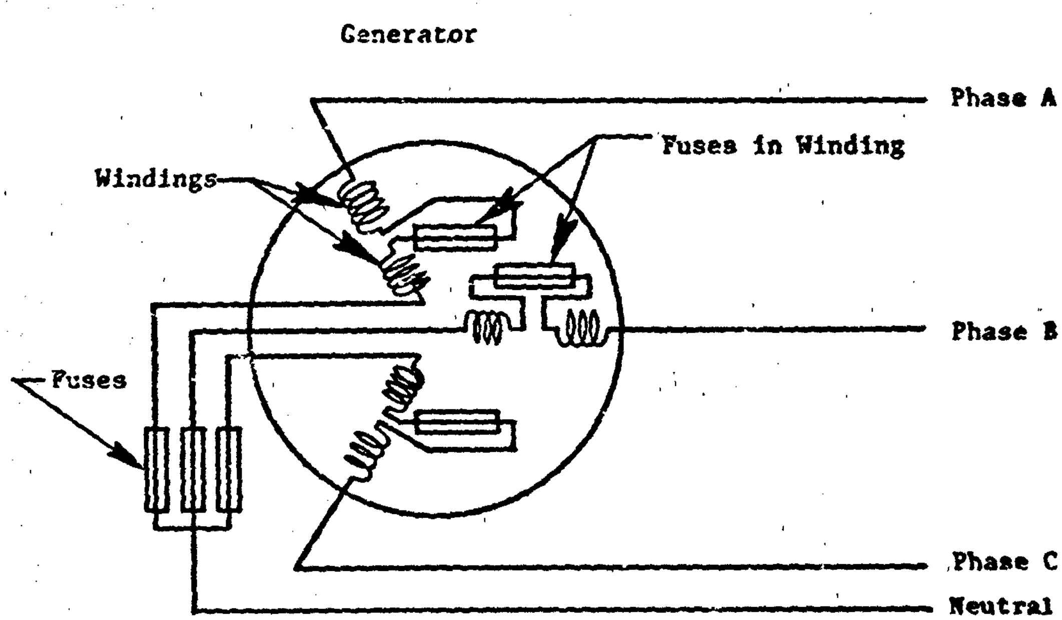


Figure 38. Doubly Fused 3 Phase Generator.

6. Selected Disconnect Approach

The fusing of the generator neutral leads provides the necessary fault protection without compromising the reliability of the engine operation for engine integration of a starter/generator. The selected fuse approach for the three levels of integration being considered is not new in concept but is being applied in an area where fuses have not previously been applied.

D. RELIABILITY AND MAINTAINABILITY CONSIDERATIONS

1. Electrical System Configuration

The Integrated Engine Generator study by definition in the contract utilizes a VSCF-type electrical system with a permanent magnet generator. This is the optimum electrical system from the standpoint of reliability and maintainability. The VSCF system utilizes a solid-state electronic converter to obtain a constant 400 Hz power from the variable-speed engine source. The alternate electrical system in common use today uses a Constant Speed Drive (CSD) to obtain the constant-frequency 400 Hz power from the variable engine speed. The alternate to the permanent magnet generator is a brushless wound-rotor machine.

a. Converter

The converter (a cycloconverter in this study) has no moving parts to wear out and thus does not require scheduled maintenance. The CSD, on the other hand, requires extensive scheduled maintenance. The reliability of the VSCF converter is also higher than the alternate CSD so there are fewer unscheduled maintenance actions required. A recent electrical system power study done under AF Contract No. F33615-78-C-2018 indicates the projected reliability of a VSCF converter with GCU (Generator Control Unit) is 20,000 hours MTBF as opposed to a projection of 6700 hours for a CSD and its GCU. The maintenance actions which are required on the VSCF converter are also minor compared to those required on a CSD. If the built-in test circuits in the VSCF indicate a failure in the converter, the unit can be removed and replaced at the flight line in twenty minutes. Repair of the VSCF converter is achieved by replacement of a plug-in module at intermediate level using BIT circuits and a test connector to identify the failed module or assembly. Over 90 percent of the electronic parts in the converter are plug-in assemblies. The failed plug-in assembly is returned to depot-level maintenance for replacement of the failed component part.

The parts used in the converter have established-reliability, JAN-TX (Joint Army-Navy Extra Testing) quality to achieve high reliability and low life-cycle costs. Use of hybrid power modules with high thermal efficiency and electrically neutral cases improves reliability and simplifies maintenance in the power-switching function of the converter. The use of Large Scale Integrated (LSI) devices and microprocessors for logic and control circuits offers continued reliability improvements for the VSCF converter.

b. Permanent Magnet Generator/Starter

The permanent magnet machine uses a solid rotor without copper wires to loosen or insulation to deteriorate. This machine's two significant advantages over a wound-rotor machine are its superior reliability and efficiency. In the IEG/S application the PM machine will not add any wearing parts to the engine. The rotor mounts onto an existing bearing-supported engine shaft. The use of fuses in place of a disconnect mechanism has simplified the PM machine devoted for engine integration.

The PM machine is obviously the most critical part of the electrical system in terms of impact on maintainability, since it is located inside the engine. To maximize machine reliability the stator windings are designed for low current density, and end-turn supports are provided to minimize the potential for breakdown at this critical area. Heavy insulation is also employed to minimize the possibility of stator faults. The net result of these extensive measures is a machine which has an MTBF of over 50,000 hours, including the fused disconnect, as substantiated by analysis in Section VII-B.

2. Engine Maintenance Action

Failure of the IEG/S does necessitate removal of the engine. This appears to be too high a price to pay for the Power Level I IEG. If the generator has a fifty- to sixty-thousand-hours MTBF, removal of the engine to replace a generator would be infrequent enough, but there is no overriding benefit for the Level I IEG. For Level II the maintenance actions saved by eliminating the IDG electrical system and starting system could very well overshadow the maintenance cost of servicing an IEG/S including engine removals.

For Level IIIA and Level III, the cost of maintenance is definitely lowered with an IEG/S. By supplying electrical power only, the aircraft secondary power accessory equipment is likewise simplified. The engine-to-aircraft interface

is simplified due to the elimination of hydraulic lines (Levels III/IIIA) and air ducts (Level III). The aircraft-to-engine (Level III) interfaces, therefore, are:

(1) fuel lines,
(2) electric wires and cables, and
(3) mounting hardware.

This reduces the engine change time on the aircraft by an estimated one-half hour for Power Level III. The electric-motor-driven fuel and lube pump can be located at a convenient, accessible location for easy maintenance. The elimination of the accessory gearbox, hydraulic lines, and air ducts improves engine maintenance through better accessibility of a "clean" engine. The assessment of maintainability of the aircraft level is necessary in order to get a true picture of the impact of an IEG/S system.

E. DESIGN-TO-COST CONSIDERATIONS

1. General

The system configuration and design features emphasize high reliability and low maintenance, which in turn yield low life-cycle costs. The emphasis on high reliability increases the initial cost of high-quality components, but this is more than compensated for by decreased maintenance and repair costs over the life of the equipment.

The material used in the machine is a prime factor in driving up the initial cost. The vanadium Permendur steel in the stator of the machine and the rare earth cobalt magnets in the rotor have both suffered severe cost increases because cobalt has been in short supply.

The reliability of the converter is not as critical as the reliability of the PM machine internal to the engine since the cost of repairing the converter is considerably lower. The use of hybrid-power SCR modules improves cooling of the semiconductor junction, yielding a significant improvement in reliability compared to discrete SCRs.

2. System IEG/S Configuration Compared to IDG

The IEG/S configuration, which uses a solid-state cycloconverter to convert the variable input speed of an engine to a constant-frequency power,

has significantly lower life-cycle costs than an IDG system, which uses a CSD hydromechanical converter to obtain a constant frequency. This is due primarily to the lower maintenance costs of a cycloconverter compared to a constant speed drive. The factors which contribute to lower maintenance costs of the solid-state converter are:

(1) Higher reliability, with much greater opportunity for improvement as electronic components continue to develop and improve.

(2) No wear parts. Life is virtually unaffected by load.

(3) Low repair costs. Time and cost of repairing or replacing of the solid-state converter are much lower than they are for the constant-speed drive.

Figure 39 shows relative cost of ownership for a VSCF-type system and an existing CSD-type system. Initial investment costs are assumed to be equal. The major difference in the two curves is due to the much lower maintenance costs of the VSCF system. The average cost of material for maintenance action is $1740 for the CSD system, which compares with $293 for the VSCF system. The average person-hours per maintenance action is 22.5 for the CSD system, which compares with 4.6 for the VSCF system. Operating cost of a VSCF system is projected at approximately 10 percent of that of an IDG system. While Figure 39 reflects the higher reliability of the VSCF system, the major cost difference is the result of the lower repair cost of VSCF. This is illustrated by Figure 40 which shows that the reliability of an IDG system would have to be ten times higher than a VSCF system to achieve the same low maintenance cost.

Specific cost comparisons for the selected IEG/S system and the systems it would replace in a typical application (A-10 aircraft) are given in Section VII-C-5 of this report.

F. AIR RESTART CONSIDERATIONS

The following is a general discussion about air restart requirements and capabilities that pertain to the Levels II, IIIA, and III IEG/S power classifications for the engines being considered. (Level I is for generation only.) The permanent magnet machine and cycloconverter combination provides the capability to generate power as well as perform engine starting in the air, which allows for different operational considerations.

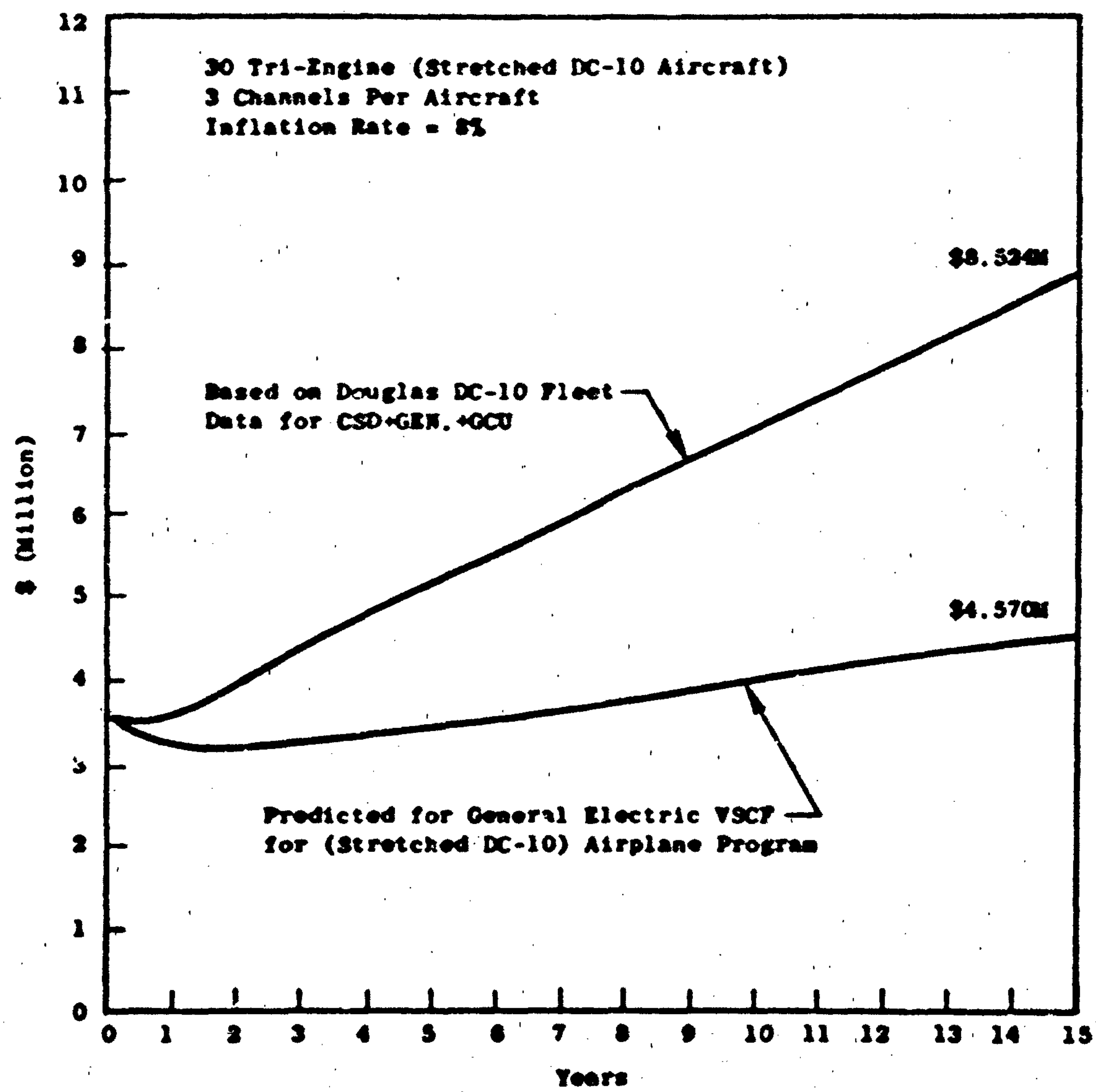


Figure 39. Cost of Ownership Based on 30 Tri-Engine (Stretched DC-10) Aircraft, 3 Channels per Aircraft, and an Inflation Rate of 8 Percent.

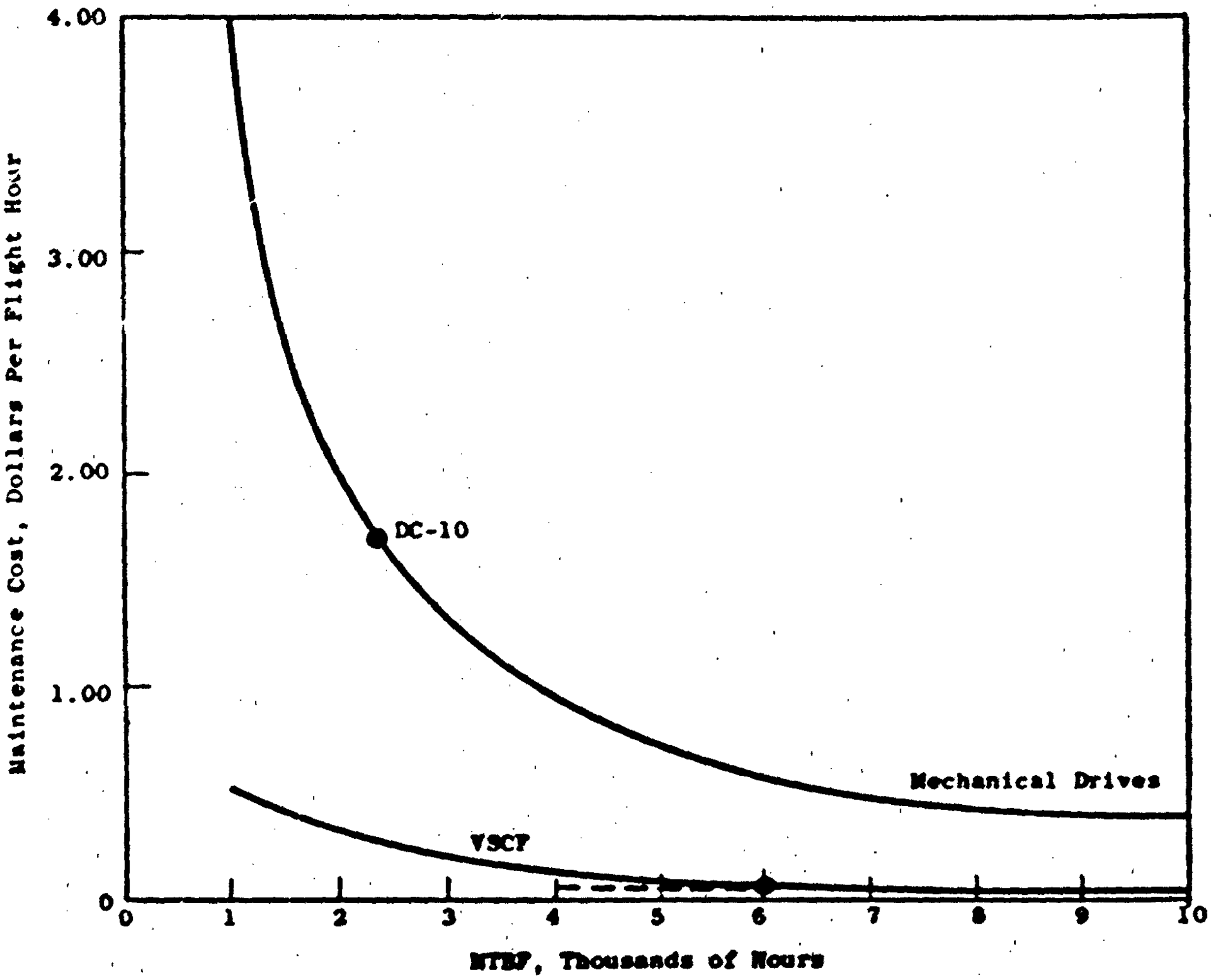


Figure 40. Maintenance Cost versus MTBF.

For all classes of engines, it is expected that the type of starting to be performed is a cross-start where one channel of the electrical system provides the essential electrical bus power as well as the electrical power to start the engine. In general, normal auxiliary power units (APUs) are sized for low emergency power extraction and are not of sufficient capacity to provide the necessary power for starting. Thus in a single-engine aircraft, the engine could not be started without being within the windmill-to-start engine envelope. In the event of applying an IEG/S Level II, IIIA, or III to a single-engine aircraft, attention must be given to the APU requirements for on-board energy storage equipment.

In multiengine aircraft applications, the IEG/S was sized in this study for 100 percent margin for twin-engine aircraft and 50 percent margin for aircraft with more than two engines. Thus the IEG/S has been sized to provide total essential bus load as well as to have sufficient capacity to perform cross-starts in the air.

As discussed in paragraph IV-B-3-b, there would be a transient degradation in the electrical system power quality until the IEG/S being started is brought up to speed. In general, the start torque requirements for an in-flight start would be reduced due to windmill assistance and decreased air density at altitude, conditions which would decrease the level of degradation during start.

G. SAFETY ANALYSIS - ELECTRICAL SYSTEM

The following analysis is presented in a format suggested by DI-H-3278 as shown in Table 41. The hazards, classifications, and probability are taken from MIL-STD-882A.

The principal hazards are those due to the 340V a.c. provided by the generator. Since this can be lethal, a means to prevent accidental contact by personnel is required, and can be satisfied by providing suitable barriers and warnings. The cycloconverter as well as the generator will probably operate at temperatures at which personnel can sustain burns. Again, barriers and warning labels are to be used. Random component failures can result in out-of-safe tolerance conditions which would normally be sensed by the protective networks that would automatically remove electrical power. Some of these protection networks do not affect the system operation, even when they have become inoperative due to a random component failure within them. However, the protection would no

Table 41. IEG/S Electrical System Hazard Analysis.

Fault/Hazard Description	Hazard Effects	Phase of Operation	Hazard Class.	Prob. Level	(A) Method of Detection (B) Corrective Action	Recommended Action
Internally shorted or accidental external shorting of terminals	Damage to associated components due to overload	G	II	D	A. Visual B. Remove power	Sense overload condition and automatically remove power.*
Effusion of toxic fumes during shorts	Injury to personnel due to inhalation	G	II	F	A. Visual B. Remove power	
Electric shock due to contact with exposed terminals	Injury or death to personnel	G	I	D	A. Visual B. Remove power	Protect terminals from accidental contact by sleeving, barriers, or guards. Place warning on guards.**
Touch temperature exceeds 60° C during and immediately after operation)	Minor injury to personnel	G	III	C	A. Touch B. Allow to cool	Place guards and warning to prevent accidental contact.
Fuse/terminal board not properly wired	Damage to system	G	III	C	A. Visual B. Remove power	Check wiring when installing or replacing generator.***
Touch temperature exceeds 60° C	Minor injury to personnel	G	III	C	A. Touch B. Allow to cool	Provide guard or cover to prevent accidental contact. Warning label indication on guard or cover showing approximate temperature. Place warning label on converter to indicate high temperature.
Converter & Generator exceed 40 pounds, the safe weight limit for one-man lift	Injury to personnel	G	III	E	A. Visual B. Refrain from lifting	Label to indicate weight and the requirement for two-man lift or for use of lifting equipment. Indicate support points for lifting equipment.
Converter acoustic noise above acceptable level	Hearing injury	G	III	E	A. Sound level B. Remove power	Personnel to use sound deadening ear muffs when working in vicinity of equipment. Place label on equipment, warning to use ear muffs.
Short circuits in power components resulting in overcurrent and smoke	Damage to equipment	G	II	D	A. Visual B. Remove power	Design to sense dangerous out-of-tolerance performance and automatically remove power.
Shattering of high-speed rotor	Damage to equipment Injury or death of personnel	A	I	E	A. Visual B. None	Design analysis or tests to ensure rotor is contained if shattered.
Loss of cooling oil	Damage to generator	A	II	D	A. Visual B. Remove power	Monitor generator temperature or oil flow and remove power when safe limit is exceeded.
Loss of cooling air	Damage to converter	A	II	D	A. Visual B. Remove power	Monitor converter temperature and remove power when safe limit is exceeded.
Networks fail to detect when safe operating limits are exceeded because of component failures within these networks	Damage to equipment	A	II	D	A. Visual B. Remove power	Perform periodic tests of these networks to ensure that they operate when out-of-limit conditions occur.

* Materials are nonflammable. No organic materials used.
** Voltage could be 360V a.c.
*** Lead connections are made with terminal. Leads would be clearly identified but could be connected improperly.

<u>Hazard Classification</u>

Class I - Catastrophic
Class II - Critical
Class III - Marginal

<u>Phase of Operation</u>

G - Ground
A - All (Ground, Takeoff, Inflight, Maintenance, and Landing)

<u>Hazard Probability Level</u>

A - Frequent
B - Reasonably Probable
C - Occasional
D - Remote
E - Extremely Improbable
F - Impossible

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longer be present, and the system would not shut down for critical failures. The protective networks should be separately tested periodically to ensure that they provide the required protection.

H. INTERFACE DESIGN CONSIDERATIONS - F404 ENGINE

Section 1.0 of "Addendum A, Drawings" contains IEG/S conceptual layouts and overall engine cross-sectional drawings defining the IEG/S interfaces and showing the relative location of the IEG/S within the F404 engine.

1. Effect Upon Engine Rotor Dynamics

No vibration analysis was performed on the F404 with integrated generator/starter system because it became apparent in the preliminary design studies that the F404 would not qualify for IEG/S integration due to space limitations as discussed in Section IV.

2. Containment of Failed Parts

No containment analysis was performed for this design due to the same reason given in the preceding paragraph.

3. Support Structure Modifications

The changes required in order to incorporate the IEG/S system into the F404 engine are tabulated in Table 42.

4. Maintenance/Overhaul Accessibility

The accessibility of the IEG/S for maintenance or overhaul requires more time:

- Engine removal from the aircraft (F404 is fuselage-mounted in the F-18). The U.S.-Navy-specified time for engine removal/replacement is 21 minutes.
- Removal of the IEG/S from the engine on the shop level. This requires qualified people, tools, and inspection equipment for engine front-end disassembly/reassembly. Removal and replacement of the fan (booster) module can be achieved in the elapsed time of 190 minutes and 390 man-minutes of labor.

TABLE 42

IEG/S ENGINE MODIFICATIONS FOR F404

POWER LEVEL	DRAWING NO./TYPE	CHANGES REQUIRED	REMARKS
I	4013186-981/cyl.	Fan case lengthened 2.5 in.; stator support flange added, cooling transfer passages incorporated. Modified bearing seal housing. New fan rotor disk stage 2. New fan rotor disk stage 3. New forward tube; new stub shaft. Extended LPT shaft.	Lengthening of fan case constitutes a major change to the engine. (Overall engine length is affected.)
	4013186-978/disk	Fan case lengthened 2.5 in. All other changes same as above.	
II	4013186-98C/cyl.	Fan case extended 2.5 in. All other changes same as under Power Level I.	Same as Power Level I
	4013186-979/disk	Fan case lengthened 3.6 in. All other changes same as under Power Level I.	
IIIA	4013271-032-cyl.	Fan case lengthened 3.6 in. All other changes same as under Power Level I.	Same as Power Level I
	4013271-033-disk	Fan case lengthened 3.6 in. All other changes same as under Power Level I.	Same as Power Level I
III	No Dwg/Cyl.	PM machine dimensions: 12.6 in. dia. x 28.5 in. length.	Considered unacceptable
	No Dwg./Disk	PM machine dimension: 14.0 in. dia. x 27.5 in. length.	

The removal of parts involves the following major components listed in the order of their removal:

- Fan modules
- Fan stub shaft and bearing support
- IEG/S module and disconnect

The installation of a new or overhauled IEG/S is completed in the reverse order.

5. Environmental Conditions at the IEG/S Location

The mid sump - the area in which the IEG/S is located - does not provide sufficient space to locate the generator/starter without significant engine modifications which result in lengthening the engine as described in Table 42.

The environmental conditions are more severe than in the high bypass engines. The air/oil (MIL-L-7898 oil) mixture within the mid-sump area is estimated to be 350° F during operation. The pressure in the mid sump is 15 to 20 psi above ambient. The temperature at startup may be as low as -65° F per military specification.

Typical engine lube oil supply (inlet) temperatures run from 200° F to 300° F. The engine lube oil flow is 9.5 gpm.

J. INTERFACE DESIGN CONSIDERATIONS - F103 ENGINE

Section 2.0 of "Addendum A, Drawings" contains IEG/S conceptual layouts and overall engine cross-sectional drawings defining the IEG/S interfaces and showing the relative location of the IEG/S within the F103 engine.

1. Effect Upon Engine Rotor Dynamics

The vibration analysis performed on the F103 was based on the layout drawing no. 4013271-030 representing the 300 KVA Power Level IIIA straddle-mounted generator rotor on its own bearing support.

A brief description of the results from this study follows.

Dynamic Responses of Low Pressure System Imbalance

This proposed design influences two modes. The first is the mid-shaft bending mode, which in the F103 is lowered from 4804 rpm to 4529 rpm for the IEG/S design. This mode is lowered due to moving the No. 2 bearing forward 3.6 inches and thereby increasing the length of the LP shaft. Lowering this

mode is not favorable because now there is only 10 percent margin above redline speed (4100 rpm). This is below accepted design practice, which specifies that 20 percent margin for calculated values is necessary. The second mode influenced by this redesign is a mode characterized by the fan rotor and nacelle translating from the fan frame. This mode is raised from 3757 to 3978 rpm and is in a favorable direction. The reason this is a more optimum location is that this mode on the F103 is observed at approximately 3700 rpm, which is at the takeoff power setting, and will now be above this power setting.

Dynamic Response of High Pressure System Imbalance

The system dynamic response associated with HP imbalance are not adversely affected by this design change.

Use of the starter/generator on the present production F103 turbofan is not feasible from a system dynamics standpoint.

Design changes to improve (raise the LP-shaft critical bending mode or depress activity by damping) the engine rotor dynamic character are hereby suggested but are not analytically substantiated:

- Increase LP shaft diameter - This change would also increase the HP shaft diameter, thereby making a major redesign of the engine necessary.
- Explore the use of squeeze film damper - This would be a feasible approach if activity at the bearing exists. Applicable only for low or moderate unbalance. Adverse effects (response) would exist in a fan-blade-out condition.
- Use a lighter fan (rotor and blades) - This would probably help if the most responsive mode involves mass coupling of the fan rotor with strain energy activity in the LP shaft.

2. Containment of Failed Rotor Parts

No IEG/S rotor burst containment analysis was performed for this application due to the multitude of conceptual IEG/S designs that would have to be covered. A general discussion on containment is found in Section VII-B.

3. Support Structure Modifications

The changes required in order to incorporate the IEG/S system into the F103 engine are tabulated in Table 43.

TABLE 43

IEG/S ENGINE MODIFICATIONS FOR F103

Power Level	Drawing Number/Type	Changes Required	Remarks
I	4013186-987/cyl.	1. Forward shroud shortened and modified 2. Fan stub shaft modified 3. Bearing support structure modified 4. Fan roller bearing moved forward 7.4 inches 5. Fan frame modified to support starter/generator and to incorporate cooling oil transfer passages 6. LP shaft lengthened by 1.5 inches 7. Bevel gear (PTO) shaft modified 8. Vent tube modified 9. Gear support housing modified	Moderate changes required
	4013186-989/disk	Changes same as above	Same as above
II	4013186-988/cyl.	Fan roller bearing moved fwd 8.3 inches: other changes same as I	Same as Power Level I
	4013186-990/disk	Fan roller bearing moved fwd 8.1 inches; other changes same as I	
IIIA		All items of drive system eliminated	
	4013271-030/cyl.	Fan roller bearing moved fwd 3.80 inches: other changes same as I	Same as Power Level I
	4013271-031/disk	Fan support roller bearing moved fwd 8.70 inches - other changes same as I All items of mechanical drive system eliminated	
III	No drawing/cyl.	Requires lengthening the engine	Considered unacceptable
	No drawing/disk	Requires lengthening the engine	

4. Maintenance/Overhaul Accessibility

The accessibility of the IEG/S for maintenance and overhaul requires more time:

- Engine removal from the aircraft. (The F103 is pylon- or tail-mounted.) Using a crew of five, engine replacement on the aircraft is estimated to require 2 to 2.5 hours for pylon-mounted engines and 3 to 4 hours for tail-mounted engines.
- For the IEG/S to be removed from and reinstalled into the engine is estimated to require 4 to 5 hours. This work has to be performed at the shop level.

The removal of parts involves the following major components, listed in the order of their removal:

- Fan module
- Fan stub shaft and bearing support
- IEG/S module and disconnect

The installation of a new or overhauled IEG/S is completed in the reverse order.

5. Environmental Conditions at the IEG/S Location

The forward sump area of the F103 provides a large space for the PM generator integration.

The environmental conditions in this cavity are acceptable for PM-machine operation. The air-oil (MIL-L-23699 or MIL-L-7808) mixture within the forward sump averages between 225° F and 275° F during operation. The corresponding pressure is 1 to 2 psi above ambient.

The minimum temperature at startup is specified at -65° F. Typical engine lube oil supply temperatures during engine operation are 165° F to 210° F. The total engine oil flow is 16.5 gpm.

K. INTERFACE DESIGN CONSIDERATIONS - TF34 ENGINE

Section 3 of "Addendum A, Drawings" contains IEG/S conceptual layouts and overall engine cross-sectional drawings defining the IEG/S interfaces and showing the relative location of the IEG/S within the TF34 engines.

1. Effect Upon Engine Rotor Dynamics

No vibration analysis was performed on the preliminary design of the straddle-mounted self-supporting generator/starter concept. However, the design which was chosen for detail design and analysis purposes is a TF34 engine with a HP-shaft-mounted generator/starter. The results of the vibration anaysis for the selected design are given in Section VII-B.

2. Containment of Failed Rotor Parts

For a discussion on IEG/S - rotor burst containment, see the analysis provided in Section VII-B.

3. Support Structure Modifications

The changes that would be necessary to incorporate the IEG/S system into the TF34 engine are tabulated in Table 44.

4. Maintenance Overhaul Accessibility

The accessibility of the IEG/S for maintenance and overhaul requires more time:

- Engine removal from the aircraft. (The TF34 is fuselage-pod-mounted on the A-10.) Using a crew of four, the estimated engine replacement time for this configuration is two hours.
- IEG/S removal from and reinstallation into the engine. This sequence is estimated to require three to four hours. This work has to be performed at the shop level.

The removal of parts involves the following major components, listed in the order of their removal:

- Fan module
- Fan stub shaft and bearing support
- IEG/S module and disconnect

The installation of a new or overhauled IEG/S is completed in the reverse order.

5. Environmental Conditions at the IEG/S Location

The A-sump area (between fan and compressor) of the TF34 provides sufficient space for the IEG/S integration. It allows packaging generator/starters of the highest considered power level (III).

TABLE 44
IEG/S ENGINE MODIFICATIONS FOR TF34

Power Level	Drawing/Type	Changes Required	Remarks
I	4013186-977/cyl.	1. Fan thrust bearing moved forward (fwd) 1.6 inches 2. Fan disk modified (larger bore). 3. New bevel gear set including new housing and bearings. 4. New fan stub-shaft. 5. LP-shaft lengthened. 6. New fan bearing support structure. 7. Fan roller bearing moved fwd 6.3 inches. 8. Fan frame modified to support starter/generator and to incorporate cooling transfer passages.	Moderate changes required
	4013186-976/disk	Fan roller bearing moved fwd 5.7 inches.	
II	4013186-972/cyl.	Same as Power Level I/cyl.	
	4013186-973/disk	Fan roller bearing moved fwd 6.6 inches. Other changes same as Power Level I.	Same as Power Level I
IIIA	4013271-026/cyl.	Fan roller bearing moved fwd 2.56 inches. All items of mechanical accessory drive system eliminated. Other changes same as Power Level I.	
	4013271-027/disk	Fan roller bearings moved fwd 2.90 inches. Other changes same as IIIA/cyl.	Same as Power Level I
III	4013271-028/cyl.	Fan roller bearing moved fwd 2.8 inches. All other changes same as IIIA/cyl.	
	4013271-029/disk	Fan roller bearing moved fwd 5.8 inches. All other changes same as IIIA/cyl.	Same as Power Level I

The environmental conditions in this cavity are acceptable for PM-machine operation. The temperature of the air-oil (MIL-17808 or MIL-23699) mixture within the A-sump area is estimated to be the same as the scavenge oil temperature, which ranges from 250° F to 300° F. The sump pressure is 1.0 to 2.0 psid above ambient. Minimum temperatures at startup can go as low as -65° F per military specifications. Oil supply temperature during engine operation stabilizes between 200° F and 250° F. The present total engine oil flow is 8.5 gpm.

L. COMPARISON/ANALYSIS OF DESIGN CONCEPTS

1. Parametric Tradeoff Analysis

The purpose of this tradeoff study is to evaluate four power levels for each of the three selected engines and to provide an indication of relative attractiveness of each IEG/S system and its application.

a. Tradeoff Study Approach

To compare the different systems, a set of trade criteria is established which uses a numerical ranking system as follows:

- Value factors rate the system on a scale from 1 to 5, 5 being the "best" value.
- The merit (weighting) factors determine the relative importance of a specific trade characteristic.
- Multiplying the value factor with the merit factor and summing it up results in a total numerical trade value. This trade value is used to compare the relative worth (payoff) of systems to each other and to a baseline configuration. The baseline configuration is defined as the production engine with conventional secondary power extraction.

b. Merit (Weighting) Factors

The secondary power system merit factors are listed in Table 45. The ranking for the established trade characteristics is based on engineering judgement and design philosophy. The merit factors are based on a scale from 1 to 10, 10 representing the most important characteristics.

TABLE 45

SECONDARY POWER SYSTEM MERIT FACTORS

	F404	F103	TF34
Weight	9	9	9
Reliability	10	10	10
Maintainability	7	7	7
System Efficiency	6	6	6
Frontal area	10	6	8
IEG/S Integration Severity	4	4	4

c. Rationale and Assumptions Behind Value Factor Ratings

The tradeoff is performed by assigning value ratings to the characteristics of each specific system.

Weight

Weights were derived from preliminary design layout drawings, existing components, and weight trend curves. Figure 41 shows IDG and cycloconverter weights as functions of system KVA ratings. Figure 42 shows the relationship between oil-cooled induction motor weights and continuous motor power rating. For determining motor weight, continuous motor power rating is 0.75 times the transient operating power requirements (50 percent overload).

High frequency feeder cables are used between the generator and the converter. Each cable consists of seven wires, twisted and shielded. A weight chart for cables is given for various system ratings in Table 46. Table 47 presents the weight summary comparison between the baseline configuration and the IEG/S configuration.

The weight value factor has the following definition:

$\frac{\Delta WT}{KVA}$	Weight Value Factor
-0.50 or less	5
-0.20 to -0.50	4
-0.20 to +0.20	3 (Base)
+0.20 to +0.50	2
+0.50 or greater	1

$$\frac{\Delta WT}{KVA} = \frac{\text{WT IEG/S System} - \text{WT IDG System}}{\text{System KVA Rating}}$$

Reliability

Reliability data for this tradeoff analysis were taken from three sources:

a) Commercial, from GE-AEG data bank

b) Military, from AFM 66-1 report

c) Analytical, for new applications

Table 48 provides reliability of engine/aircraft accessory line replacement unit (LRU) from commercial service of CF6 (F103) engines. Table 49 provides field reliability report data for CSD-generator units reported in the military 66-1

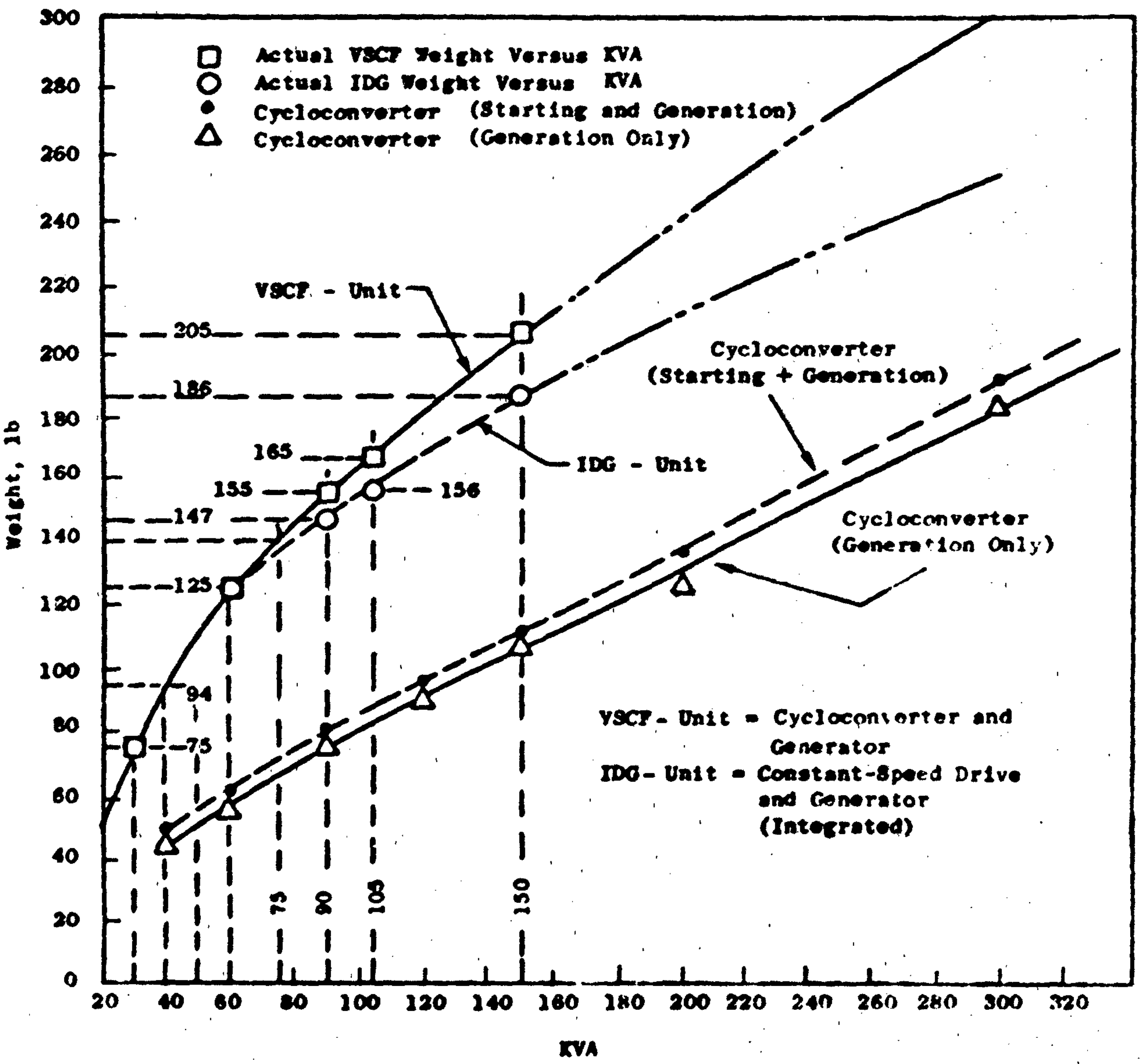

Figure 41. Weight versus Power for IDG and Cycloconverters.

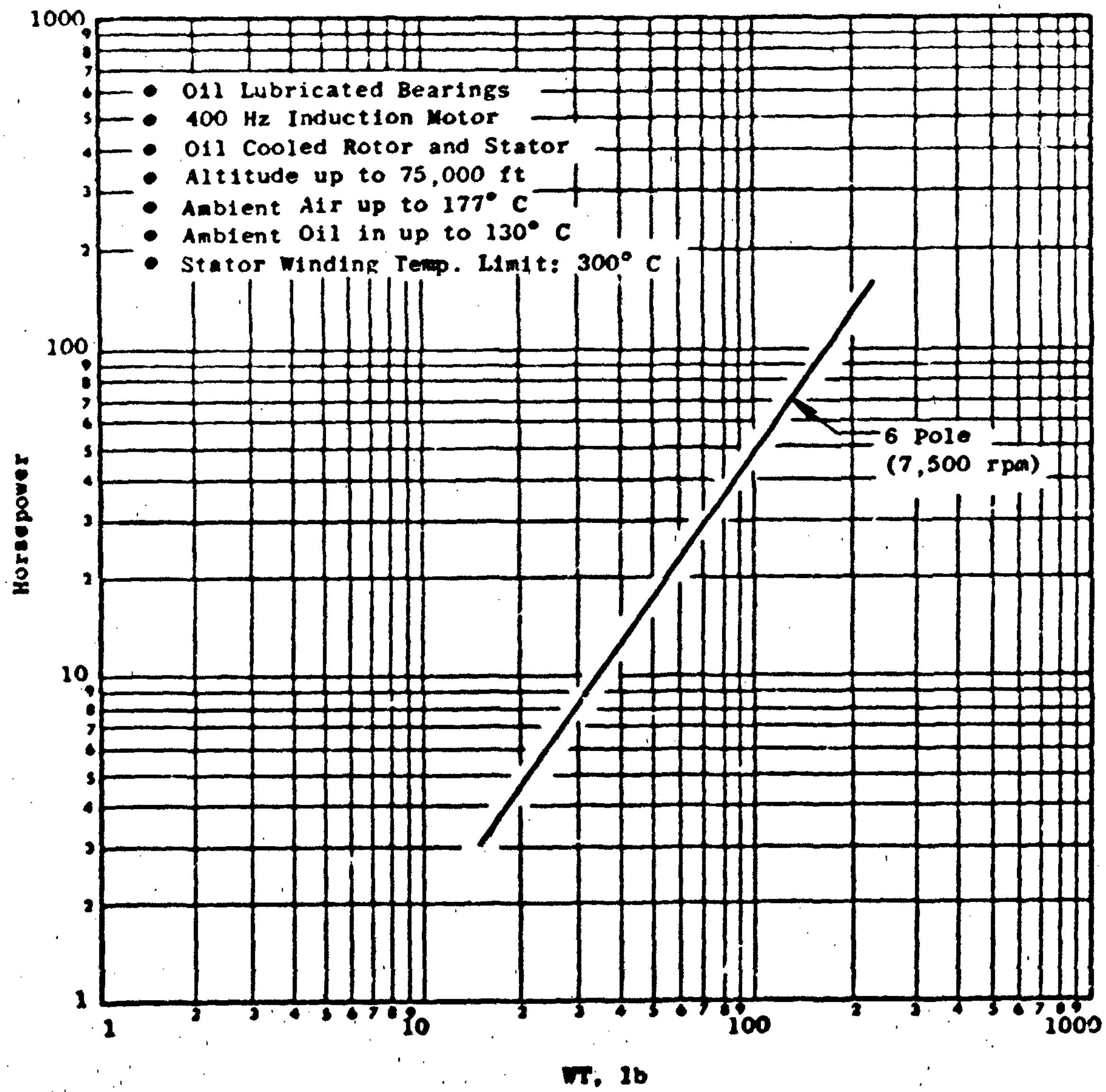

Figure 42. Weight versus Horsepower for Oil-Cooled, Six-Pole, Electric Induction Motor.

TABLE 46

HIGH FREQUENCY POWER CABLE WEIGHTS

System	Qty.	Individual Wire Size	lb/ft
40	2	#12	0.4
75	4	#12	0.8
90	3	#10	0.9
120	4	#10	1.2
200	8	#10	2.4
300	12	#10	3.6

TABLE 47

WEIGHT COMPARISON BETWEEN BASELINE AND IEG/S CONFIGURATION

Engine	F404				F103				TF34			
Accessory Drive System, lb	62				290				134			
Air Starter, lb	28				34				22			
Air Start Valve, lb	8				10				7			
Air Duct (start), lb	5				6				4			
IDG KVA/lb	75 KVA/136				90 KVA/147				40 KVA/78			
									Bleed Air Duct (5)			
IDG - System lb - Base	239				487				245 (250 III)			
Power Level	I	II	IIIA	III	I	II	IIIA	III	I	II	IIIA	III
KVA	75	90	200	800	90	120	300	1200	40	60	75	120
Cycloconverter, lb	72	80	136		75	96	190		46	62	72	96
IEG/S Cyl. PM-Machine	86	100	169		132	171	270		60	73	91	108
ΔEng. Structure, lb	16	17	18		34	36	38		9	9	9	10
ΔEng. Shafting, lb	8	8	8		8	8	8		5	5	5	5
ΔAccessory Drive System, lb	55	52	-		261	246	-		121	114	-	-
	(-10%)	(-15%)	(-100%)		(-10%)	(-15%)	-		(-10%)	(-15%)	-	-
Air Starter + Valve + Duct, lb	41	---	-		50	-	-		33	-	-	-
Feeder Power, lb	3.0	3.5	10.0		4	5	14		1.5	2.0	2.5	3.5
Cable Length		------4 ft -------				------5 ft -----				--------- 3 ft ---------		
Fuel/Lube F_p Motor, lb			90				140				40	40
Horsepower at Takeoff, hp			61				115				18	18
IEG/S - System, lb	281	261	431	----	564	562	660	----	276	265	220	273
ΔW = (IEG - IDG)	+42	+22	+192	----	+77	+75	+173	----	+15	+4	-25	+23
ΔW/KVA	0.56	0.24	0.96		0.85	0.62	0.58		0.37	0.06	-0.33	0.19
Weight Value Factor	1	2	1		1	1	1		2	3	4	3

TABLE 48

RELIABILITIES OF ACCESSORIES AND RELATED COMPONENTS

Line Replaceable Unit	Delays/Cancel Per 10^6 Hours	Replacements Per 10^6 Hours
Hydraulic Pump	80	890
Starter	41	179
Fuel Control	37	117
Lube Pressure Sensor	19	40
Lube & Scavenge Pump	12	15
Starter Air Valve	8	171
Fuel Pump	5	47

TABLE 49

CSD-SYSTEM RELIABILITY (MTBF-HOURS); 66-1 DATA FOR SIX MONTHS ENDING 30 APRIL 1979

Aircraft

System Part	A-7D	B-52G	G-52H	E-3A	KC-135	F-4D	F-4E	C-5A	C-9A	A-10	F-11D
Generator	1322	1244	1104	10,704	500	1652	1154	1384	6737	21,539	832
CSD	4627	902	603	4,312	750	885	853	1615	3369	2,019	979
Control	1815	1067	1196	2,155	16,202	1982	3838	577	4491	2,084	4160
Frequency & Logic Control	-	-	-	15,092	-	4956	1371	-	-	-	-
Voltage Regulator	-	5123	2659	-	1,500	-	-	-	-	-	-
Electrical System Reliability MTBF - Hrs.	656	474	495	689	294	409	361	low	high	979	405
Total Aircraft Hours	46,268	32,021	17,964	3,737	108,013	49,556	76,750	24,220	-	32,309	8,319

report. Through elimination of the highest and lowest value of the above data, an average MTBF of 1742 hours results for the CSD and 2903 hours MTBF for the generator. This is equivalent to 574 failures per 10^6 hours for the CSD and 344 failures per 10^6 hours for the generator.

Cycloconverter field reliability is reported to be 80 failures per 10^6 hours or an MTBF of 12,500 hours for generation only and 100 failures per 10^6 hours for the more complex starting/generating mode.

An electric induction motor reliability of 100 failures per 10^6 hours (10,000 hours MTBF) looks realistic for a maximum transient speed of 7500 rpm.

Unscheduled engine removals (UER) due to accessory drive system failures are listed in Table 50. Data from the TF34 engine are used for tradeoff study purposes.

The goal of IEG/S reliability was set by GE at 50,000 hours MTBF including the safety disconnect. The self-supported generator/starter with mechanically resettable disconnect, shown in Figure 43, did not meet this goal; therefore, a new design with an HP-shaft-mounted generator/starter rotor and an electrical disconnect was chosen and is analyzed in Section VII-B. The results show that an MTBF of 50,000 hours (or a failure rate of $20/10^6$ hours) is achievable.

The reliability of the PM disk machine was not analyzed. However, higher mechanical complexity and higher parts-count contribute to lower reliability. The reliability of the PM disk machine was arbitrarily chosen to be 20 percent lower than the cylindrical PM machine, which would make it 24 failures per 10^6 hours.

Table 51 compares the reliabilities of conventional secondary power extraction systems to the IEG/S concept.

The reliability value factor has the following definition:

UER x 10^{-6} Hours	Individual Reliability Value Factor	LRU x 10^{-6} Hours
<20	5	<200
21 - 30	4	201 - 400
31 - 40	3	401 - 600
41 - 50	2	601 - 800
>50	1	>800

The sum of individual UER rating plus LRU rating is used in the final tradeoff matrix for the combined reliability value factor.

TABLE 50

ENGINE DRIVE SYSTEM RELIABILITIES -
UNSCHEDULED ENGINE REMOVALS
(UER)/10^6 HOURS

	UER TF34/S3A	UER F103 (CF6-50)	UER CF6-6
ENGINE FLIGHT HOURS	134,802	1,851,113	1,616,389
PERIOD	7/77 to 7/78	3/78 to 2/79	3/78 to 2/79
POWER, TAKEOFF	7.42	7.56	3.71
TRANSFER GEARBOX	-	1.62	4.33
ACCESSORY GEARBOX	22.25	1.08	1.85
TOTAL, $\frac{\text{UER}}{10^6 \text{ hours}}$	29.7	10.26	9.89
DRIVE SYSTEM MTBF, hours	34,000	97,000	100,000

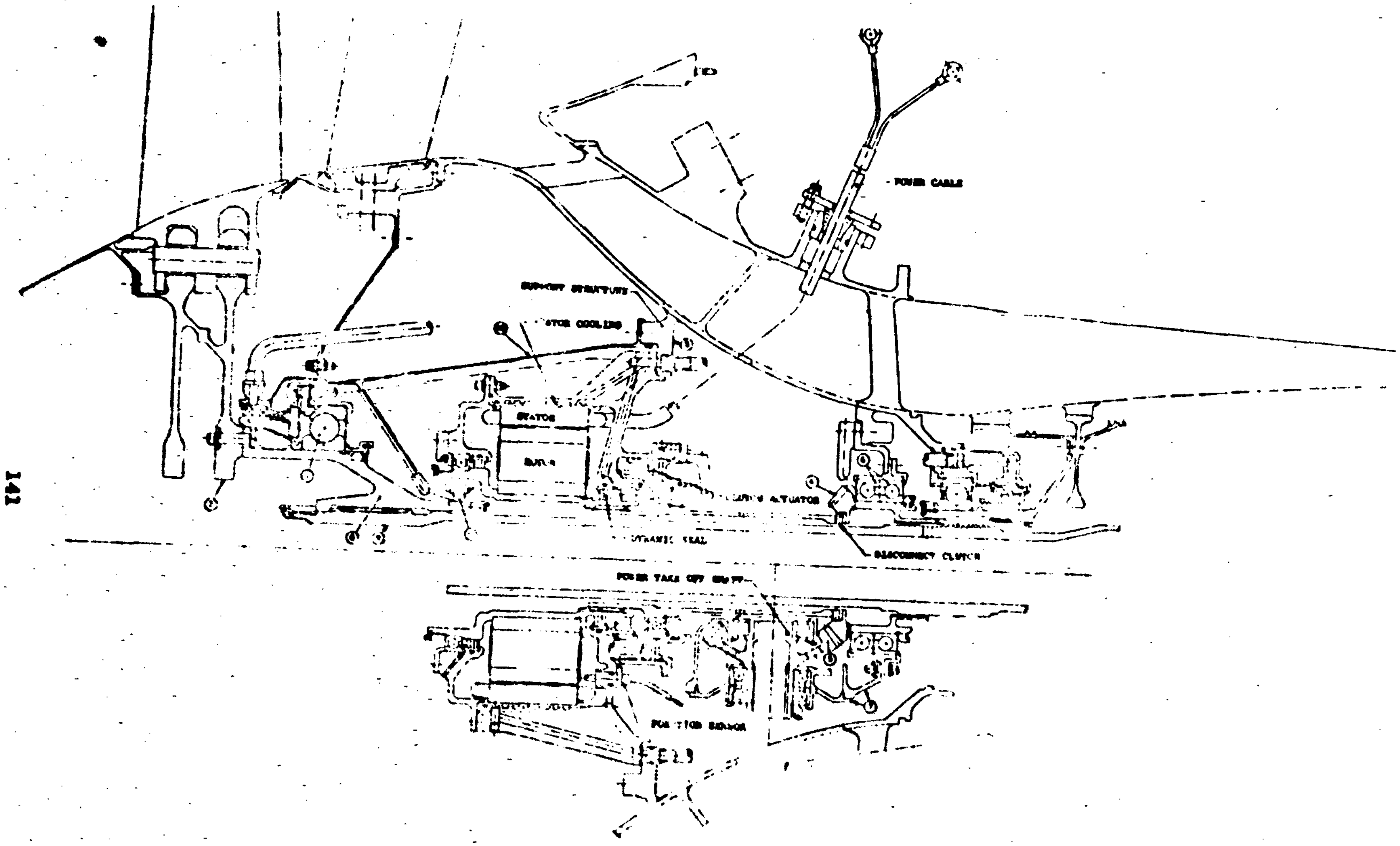

Figure 13. IES/G Configuration Study, Cylindrical Machine, Level II, TF34.

TABLE 51
COMPARISON OF SECONDARY POWER SYSTEM
RELIABILITIES - FAILURES PER 10^6 HOURS

SYSTEM	COMPONENT		BASELINE CSD SYSTEM		IEG/S SYSTEM - TF34, F404, F103							
POWER LEVEL			I		I		II		IIIA		III	
REPLACEMENT LEVEL*	UER	LRU	UER	LRU	UER	LRU	UER	LRU	UER	LRU	UER	LRU
PTO TGB AGB	30		30		30		25					
CSD		574		574								
Generator		344		344								
Cycloconverter Generation		80				80						
Cycloconverter Starter/ Generator		100						100		100		100
Air Starter		179		179		179						
Air Starter Valve		171		171		171						
IEG/S (Cylinder) Electrical Disconnect	20				20		20		20		20	
Induction Motor		100								100		100
Total Removals per 10^6 Hours			30	1268	50	430	45	100	20	200	20	200
Reliability Value Factor			4	1	2	3	2	5	5	5	5	5
Σ Value Factor			Base 5		5		7		10		10	

*UER - Unscheduled Engine Removal
LRU - Line Replacement Unit

As can be seen from the results of Table 51, Power Levels I and II are not favorable in respect to the UER rates. Power Levels IIIA and III show definite improvement in overall reliability. Expected LRU removal rate drops by a factor of 6.3 for the IEG/S Power Levels IIIA and III.

Maintainability/Accessibility

The maintainability/accessibility value factors are defined as follows:

Item	Maintainability Value Factor
Internal engine-mounted components	1
Core location (high bypass)	2
External frame	3 to 5

System Efficiency

The following typical average component efficiencies were used:

Mechanical drive system:	$\eta_M = 0.97$	
IEG/S PM cylindrical machine efficiency:	IEG = 0.93	
Cycloconverter	$\eta_{cyc} = 0.95$	$\eta_{VSCF} = 0.78$
Wound rotor generator, gearbox mounted:	$\eta_G = 0.85$	
CSD hydromech. drive/without generator:	$\eta_{CSD} = 0.88$	$\eta_{IDG} = 0.75$
Electric induction motor efficiency:	$\eta_{EM} = 0.88$	

Based on the above efficiency data and a PF of one, the overall system efficiency is derived as shown in Table 52. The fuel (and lube) pumps are not included in the efficiency tradeoff. However, a variable-speed electrically driven fuel pump improves pumping efficiency considerably. Fixed-displacement fuel pumps driven off the accessory drive-train, for example, require flow bypass control, which results in undesirable fuel temperature rise and less engine oil cooling capability.

Gear pump efficiencies of 80 to 85 percent could be realized without bypass requirements. This compares with 70 to 75 percent pumping efficiency for a 10 percent bypass ratio. The Power Level II efficiency is not listed because starter efficiency is not analyzed and the generation level is equal to Power Level I. It was not within the scope of this study to utilize the excess power generation capacity of the starter. Optimization of Power Level II has a higher potential

TABLE 52

COMPARISON OF SYSTEM EFFICIENCIES AT RATED POWER

	Component Efficiency	Baseline CSD			Power Level I			IEG Power Level IIIA			Power Level III
		TF34	F404	F103	TF34	F404	F103	TF34	F404	F103	TF34
Electric Power		40	75	90	40	75	90	75	200	300	120
Drive-Sys. Mech. Power, kW_M		75	200	300	35	125	210	-	-	-	-
Drive System Losses											
$kW_L = kW_M \times (1 - \eta_M)$	$\eta_M = 0.97$	2.25	6.0	9.0	1.0	3.7	6.3	-	-	-	-
Wound Rotor Generator											
$kW_L = kW_E \times (1 - \eta_G)$	$\eta_G = 0.85$	6.0	11.2	13.5	-	-	-	-	-	-	-
CSD Hydromech. Drive											
$kW_L = \frac{kW_E}{\eta_G} (1 - \eta_{CSD})$	$\eta_{CSD} = 0.88$	5.6	10.6	12.7	-	-	-	-	-	-	-
Cycloconverter											
$kW_L = kW \times (1 - \eta_{cyc})$	$\eta_{cyc} = 0.95$	-	-	-	2.0	3.8	4.5	3.75	10.0	15.0	6.0
IEG/S PM Machine											
$kW_L = \frac{kW}{\eta_{cyc}} \times (1 - \eta_{IEG})$	$\eta_{IEG} = 0.93$	-	-	-	3.0	5.5	6.6	5.5	14.7	22.1	8.8
El. Motor Power (kW)		-	-	-	-	-	-	(14)	(45)	(86)	(14)
$kW_L = kW\ pump \times (1 - \eta_{EM})$	$\eta_{EM} = 0.88$	-	-	-	-	-	-	1.7	5.4	10.3	1.7
Total Losses kW		13.8	27.8	35.2	6.0	13.0	17.4	11.0	30.1	47.4	16.5
Overall Eff. $\frac{kW\ out}{kW\ in}$		0.84	0.88	0.89	0.93	0.94	0.95	0.87	0.87	0.86	0.88
System Efficiency Value Rating		3.0	3.0 Base	3.0	5.0	5.0	5.0	4.0	3.0	1.0	5.0

than the results of this study indicate.

The system efficiency value rating is based on the overall system efficiency change in comparison to the baseline configuration as determined below:

System Efficiency Difference With Baseline	Efficiency Value Factor
Degradation	1 - 2
Base, 0	3
Improvement	4 - 5

Frontal Area

The frontal area variation of an engine due to accessory drive components and accessory installation depends on the particular engine configuration. In the case of wing- or fuselage-pod-mounted engines, the nacelle aerodynamic drag is directly affected by the engine frontal area. The highest benefit in installed engine performance (drag reduction) is for aircraft operating at near-sonic speeds, applicable to low bypass engines (F404) or pure jets.

With turbofan engines, the gearbox is normally located either on the circumference of the fan case (F103) or on the circumference of the core (TF34). Locating the gearbox on the fan case contributes directly to engine frontal area. With the gearbox on the engine core, frontal area is not affected because it is hidden in the engine shadow.

Frontal area value rating for the base engine is based on a percentage of engine frontal area. Frontal areas (Table 53) are derived from existing engine information. The definition of the frontal area value rating for the base engine follows:

Drive System % of Engine Area	Frontal Area Value Rating
< 2	5
2-5	4
5-8	3
8-12	2
> 12	1

TABLE 53
COMPARISON OF DRIVE SYSTEM FRONTAL AREAS

Engine	TF34	F404	F103
AGB location	Core	Mid Frame	Fan Frame
Engine max dia., in.	49.0	34.8	92.0
Engine frontal area, $in.^2$	1885	951	6648
Drive system % of engine area outside of engine shadow	within engine envelope	20.0	10.5
Frontal area value rating (base)	5	1	2

TABLE 54
GUIDELINES FOR IEG/S SEVERITY RATING

Changes Required	Integration Severity Rating
• Major	
Air flow path modification	no-go
Stretched engine >5 inches	no-go
Stretched engine <5 inches	1
Increased LP-shaft diameter	
Increased main bearing size diameter	
• Moderate	
Generator stator support (fan case)	2-3
Shaft modification (stub-, LP-, HP-shaft)	
• Minor	
Coolant and lube supply ports	4
Electric wiring channels	
Accessory drive	
Bearing support housing	
• Base	5

IEG/S Integration Severity

Engine changes required to integrate the IEG/S were determined from preliminary layouts. A summary of required changes is listed under "Support Structure Modifications," which appears as subsection 3 of Sections VI-H, VI-J, and VI-K.

The IEG/S integration severity rating is based on the guidelines shown in Table 54.

2. Tradeoff Study Evaluation

Tables 55 through 57 present the computation of trade values in matrix form. Comparisons are shown between the tradeoff values of the IEG/S system and the baseline engine. In each of the tables, the TF34 is shown to have the highest absolute trade value of the three candidate engines. A comparison between the three tables indicates that the IEG/S system for Power Levels III and IIIA offers the highest payoff relative to the baseline configuration.

TABLE 55 TRADEOFF MATRIX - POWER LEVEL I	TF34					F404					F103 (CF6-50)				
	Merit Factor	Value Factor		Product		Merit Factor	Value Factor		Product		Merit Factor	Value Factor		Product	
		Base	IEG/S	Base	IEG/S		Base	IEG/S	Base	IEG/S		Base	IEG/S	Base	IEG/S
Weight	9	3	2	27	18	9	3	1	27	9	9	3	1	27	9
Reliability	10	5	5	50	50	10	5	5	50	50	10	5	5	50	50
Maintainability/ Accessibility	7	2	1	14	7	7	3	1	21	7	7	4	1	28	7
System Efficiency	6	3	5	18	30	6	3	5	18	30	6	3	5	18	30
Frontal Area	8	5	5	40	40	10	1	2	10	20	6	2	3	12	18
IEG/S Integration Severity	4	5	3	20	12	4	5	1	20	4	4	5	3	20	12
Total Value				169	157				146	120				155	120

TABLE 56 TRADEOFF MATRIX - POWER LEVEL II	TF34					F404					F103 (CF6-50)				
	Merit Factor	Value Factor Base	Value Factor IEG/S	Product Base	Product IEG/S	Merit Factor	Value Factor Base	Value Factor IEG/S	Product Base	Product IEG/S	Merit Factor	Value Factor Base	Value Factor IEG/S	Product Base	Product IEG/S
Weight	9	3	3	27	27	9	3	2	27	18	9	3	1	27	9
Reliability	10	5	7	50	70	10	5	7	50	70	10	5	7	50	70
Maintainability/ Accessibility	7	2	1	14	7	7	3	1	21	7	7	4	1	28	7
System Efficiency	6	3	5	18	30	6	3	3	18	18	6	3	2	18	12
Frontal Area	8	5	5	40	40	10	1	3	10	30	6	2	4	12	24
IEG/S Integration Severity	4	5	4	20	12	4	5	1	20	4	4	5	3	20	12
Total Value				169	186				146	147				155	134

TABLE 57 TRADEOFF MATRIX - POWER LEVELS III AND IIIA	TF34 (Power Levels III & IIIA)					F404 (Power Level IIIA)					F103 (CF6-50) (Power Level IIIA)				
	Merit Factor	Value Factor Base	Value Factor IEG/S	Product Base	Product IEG/S	Merit Factor	Value Factor Base	Value Factor IEG/S	Product Base	Product IEG/S	Merit Factor	Value Factor Base	Value Factor IEG/S	Product Base	Product IEG/S
Weight	9	3	3(4)	27	27(36)	9	3	1	27	9	9	3	1	27	9
Reliability	10	5	10	50	100	10	5	10	50	100	10	5	10	50	100
Maintainability/ Accessibility	7	2	1	14	7	7	3	1	21	7	7	4	1	28	7
System Efficiency	6	3	5(4)	18	30(24)	6	3	3	18	18	6	3	1	18	6
Frontal Area	8	5	5(5)	40	40	10	1	5	10	50	6	2	5	12	30
IEG/S Integration Severity	4	5	3	20	12	4	5	1	20	4	4	5	3	20	12
Total Value				169	216(219)				146	188				155	164

SECTION VII

DETAILED DESIGN DEFINITION OF SELECTED IEG/S AND ENGINE

This section provides information on the rationale used to determine the final engine/power level selection. Furthermore, the results of an in-depth design analysis relevant to the selected configuration are discussed and complemented by explanatory descriptions of major PM machine components. Interfaces between the engine and the starter/generator are defined by design layouts. The advantages and disadvantages of the IEG/S design are compared to those of the baseline engine.

A. BASIS FOR SELECTION OF TF34 ENGINE AT LEVEL III ELECTRICAL POWER EXTRACTION

A multitude of electric-only and hybrid secondary power extraction systems based on the IEG/S concept were studied and numerically ranked for three engine categories. The results of this tradeoff evaluation performed in Section VI-L assisted the selection of the TF34 Power Level III combination.

It is appropriate to caution the reader not to conclude that the selected engine category/power level is an optimum for all applications. This study provides ample information to apply the guidelines used in this report for other candidate IEG/S applications which might be considered.

Major considerations which led to the selection of the TF34 with Power Level III and a cylindrical PM machine are summarized below:

TF34

- The TF34 engine is a widely used engine in the USAF, and is therefore readily available from AF inventory for IEG/S integration.
- Because they are small, the TF34 engine and the integrated electric starter/generator are less costly and easier to build than all other candidate configurations investigated.
- The TF34 is presently used on multiengine aircraft (A-10), an advantage for the flight-test phase. Moderate changes to the baseline engine are required for IEG/S incorporation.

Power Level III

The highest possible power level possesses the potential for maximum payoff. The TF34 is the only study-engine to allow Power Level III integration.

Cylindrical PM Machine

The selection of the cylindrical PM machine over the disk-type machine has the following rationale:

- simple design
- lower development risk
- good stator cooling
- fewer parts
- higher reliability
- more design flexibility
- easier assembly
- lower cost

Further design considerations adopted for the selected design are electrical (fused) disconnect and HP-shaft-mounted generator/starter rotor.

B. DESCRIPTION OF SELECTED GENERATOR/ENGINE INTERFACE

Figure 44 shows the location and general arrangement of the integrated generator/starter within the TF34 engine.

The generator permits a Power Level III secondary electric power extraction of 120 KVA (system). The PM machine functions as a starter (motor) and starts the TF34 in 23 seconds when limited to 90 KVA input power.

1. Design Description, General

The IEG/S is an oil-cooled permanent magnet machine which provides rated system power over the speed range of 9380 rpm (min idle at -65° F, S.L. static) to 17,600 rpm (takeoff condition). The IEG/S is located in the forward sump (A-sump) area with the IEG/S rotor mounted directly to the HP shaft. Engine protection is provided in the form of an electrical safety disconnect described in Section VI-C.

2. Engine/Generator Interfaces

The IEG/S unit is integrated into the existing TF34 with moderate modification requirements.

The forward frame (fan frame) provides the structural support for the IEG/S. Four hollow fan frame struts are used to pass cooling oil lines and electric cables, as well as engine service lines such as lube oil, seal pressurizing air,

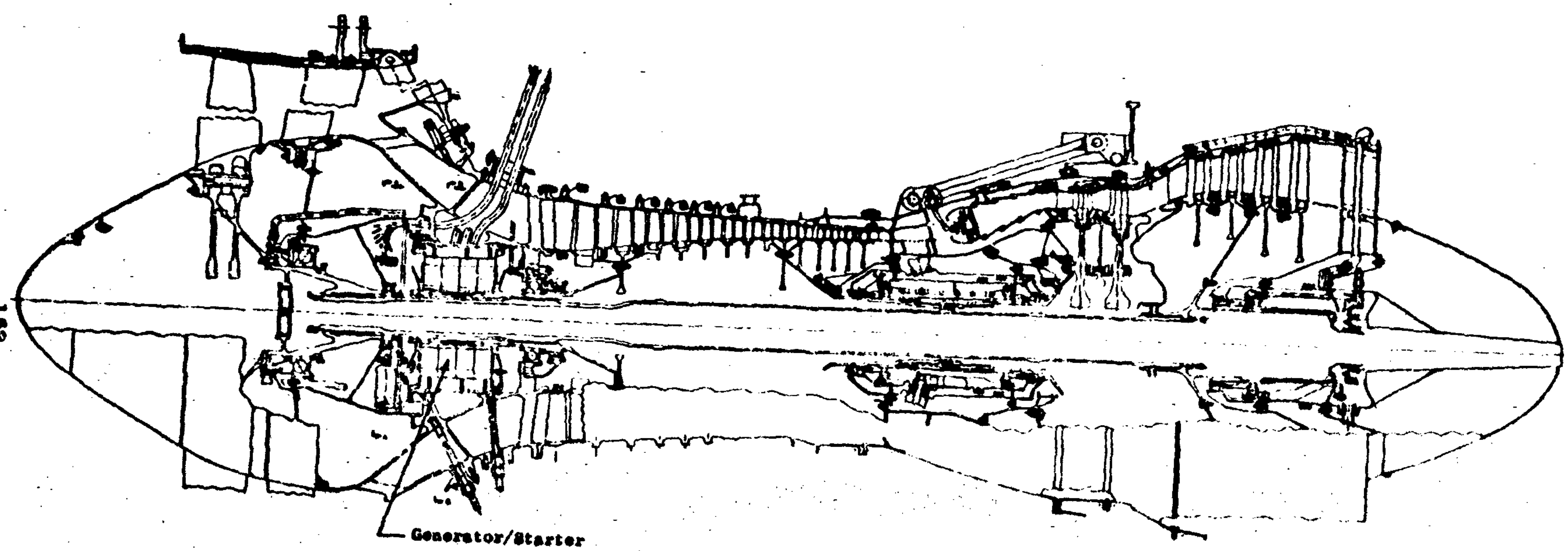

Figure 44. 120 KVA/TF34 Shaft-Mounted Permanent Magnet Machine.

and lube scavenge lines. Figure 45 identifies the above-mentioned transfer lines and their respective strut locations. No change is required to the exterior strut shape within the flowpath. The electric harness is part of the fan frame and does not require removal during generator replacement. The electric harness passes through the top strut and is sealed as shown in Figure 45.

Nine power leads, connected to the power terminals on the f... support ring, combine into six cables of approximately 0.50 inch diameter each, which lead to the engine exterior. The seal between the housing and the six cables is achieved by an intermediate plug which is permanently bonded to the cable bundle (harness). The cooling oil connections are made automatically at the time of generator/starter installation. Oil collector grooves on either end of the stator housing correspond with their respective inlet and outlet ports. The structural support of the generator/starter is achieved through a hub-like structure integrated into the fan frame which provides the radial and axial support as well as providing for torque reactions. The generator/starter rotor is mounted rigidly onto the HP-shaft extension supported by the main shaft engine bearings. For easy installation of the generator rotor subassembly, an intermediate sleeve is used to retain the four individual rotor disks. Torque is transferred through a fixed spline connection (which does not have to accommodate for any misalignment) on the HP shaft and a key on the disk side. The disks are shrunk-fit onto the sleeve. The generator rotor module is retained on the HP shaft by a spanner nut with mechanical locking device.

3. Generator/Starter Cooling

Cooling of the IEG/S is provided by engine oil which is circulated through cooling passages around the PM machine stator. The heat from the generator rotor is dissipated through conduction into the HP-shaft and the air-oil mixture of the A-sump. Oil slingers on either end of the rotor deflect the oil away from the end turns to prevent erosion caused by the impact of high velocity oil droplets. Engine oil has an inlet temperature ranging from 200 to 250° F and is supplied by the engine lube oil pump.

The required cooling oil flow (Q - gpm) depends on the heat removal rate (q - BTU/min.) and the allowable coolant (oil) temperature rise (ΔT). The heat removal rate is defined by the following relationship:

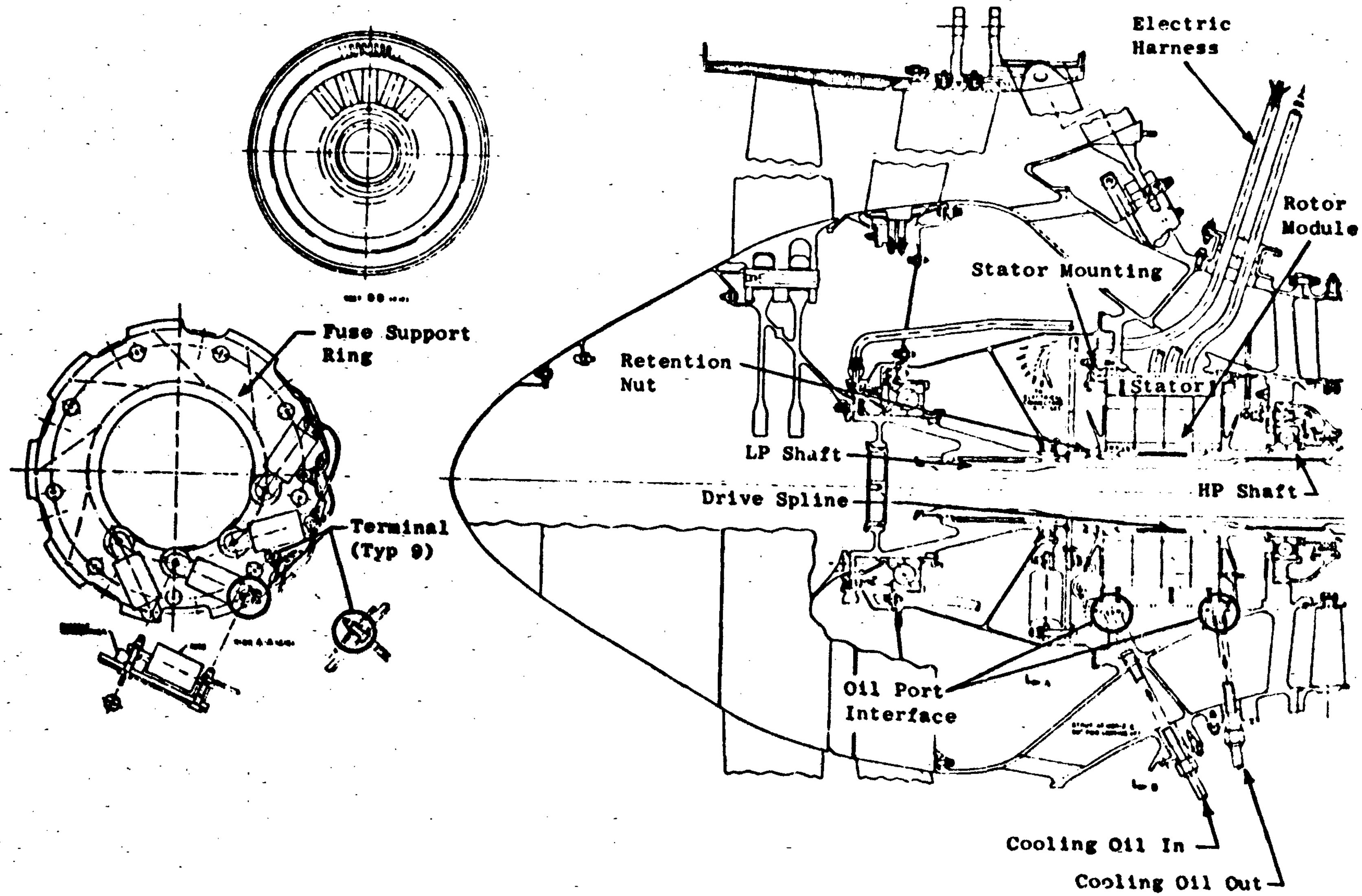

Figure 45. 120KVA / TF34 IEG/S Interfaces.

$$q = \dot{m} \times C_P \times \Delta T \qquad \text{\{BTU/min.\}}$$

$$q = kW_L \times 57 \qquad \text{\{BTU/min.\}}$$

kW_L = Power loss {kW}

$\dot{m}$ = Mass flow {lb/min.}

C_P = Specific heat {BTU/lb.- ^{o}F}

$\Delta T = T_{out} - T_{in}$ = Allowable temperature rise of coolant {^{o}F}

The cooling oil flow is defined by the following relationship:

$$Q = \frac{\dot{m}}{\rho} = \frac{57 \times kW_L}{C_\rho \times \Delta T \times \rho} \qquad \text{\{gpm\}}$$

where ρ = coolant density {lb/gal}.

Engine lube oil (MIL-L-7808) at 220° F has a specific heat C_P = $0.50 \frac{BTU}{lb-° F}$ and a density $\rho = 7.34 \frac{lb}{gal}$

The cooling oil first passes through the generator cooling circuit and then continues to the engine lube points for engine component lubrication and cooling. The oil-temperature rise in the generator cooling circuit therefore has to be kept low both to provide sufficient lubricating film for moving parts and to prevent premature oil degradation.

For the maximum determined IEG/S generator loss of 8.6 kW at max power and speed and an assumed oil temperature rise of $\Delta T = 30°$ F, the required cooling oil flow $Q = \frac{57 \times 8.6}{0.5 \times 30 \times 7.34} = 4.5$ gpm.

The baseline engine lube oil flow rate is 8.5 gpm. The flow velocity in pressurized lube passages (lube lines) is typically 16 ft/sec which requires a flow area of 0.09 in.^{2}, equivalent to a 3/8-inch-diameter tube.

4. TF34 - IEG/S Engine Dynamics

The purpose of this investigation was to determine the dynamic behavior of the modified TF34 engine with HP-shaft-mounted generator/starter rotor and, once these dynamics have been determined, to compare them to those of the baseline TF34-100 engine.

a. Results

The results in Tables 58 and 59 are a comparison of the resonances obtained for the baseline and the IEG/S configuration, under 1/rev high pressure (HP) Rotor and low pressure (LP) Rotor excitation, respectively. In addition, Figure 46 displays the No. 3 bearing load for 25 gram-inches at the IEG/S rotor.

b. Conclusions

As a result of the analysis, the following conclusions can be drawn:

- LP reference resonances are relatively unaffected by the introduction of a generator/starter.
- The HP reference resonances are not significantly affected, but the casing/gas generator (CG) bending mode and compressor/B-sump translation mode characters are affected in the following ways:
  - The casing/CG bending mode is transformed in character to a casing-IEG/S translation resonance with six times more total percentage of the total energy in the No. 3 bearing for the IEG/S configuration.
  - The compressor/B-sump translation resonance is unaffected by the IEG/S since the PM machine is located at a node for this resonance. However, the percent potential energy in the No. 3 bearing is reduced by a factor of 4 for the IEG/S design.
- The TF34-IEG/S design will produce high No. 3 bearing loads from 14,000 to 16,000 rpm because of the sensitivity of the fan case/fan frame and fan frame/A-sump resonance caused by generator rotor unbalance.
- The IEG/S integration is acceptable except for the possibility of high No. 3 bearing loads at idle due to generator-rotor and/or High Pressure Turbine (HPT) unbalance (Figure 47), either of which would affect the No. 3 bearing life.

c. Recommendations

The following recommendations are suggested to lower the No. 3 bearing loads:

- The IEG/S rotor must be well balanced, since it will produce high No. 3 bearing loads at gas generator idle.

TABLE 58

RESONANCE COMPARISON BETWEEN
TF34 BASE AND IEG/S DESIGN
1/REV HIGH PRESSURE EXCITATION

MODE CHARACTER	TF34 BASE	TF34 IEG/S
Front Frame	1201	1253
Engine Pitching, Rear Mount	1902	1897
Engine Transmission	2470	2513
Fan Case/Fan	2924	2997
Front Frame/A Sump	3485	3841
HPT/Casings/Front Frame	4140	5360
Casings/LP Shaft	4712	4353
Fuel Control	7130	7212
Fan/LP Shaft	8363	7475
LP Shaft/LPT/Fan	8531	8476
Casings/GG Bending	9370	9965
Fan Case/Front Frame	13362	14365
Front Frame/A Sump	14565	16872
LPT Bending/C Sump	15575	15449
Compressor/B Sump Transmission	18121	18140
LPT Shaft Bending	18893	17346

TABLE 59

RESONANCE COMPARISON BETWEEN
TF34 BASE AND IEG/S DESIGN
1/REV LOW PRESSURE EXCITATION

MODE CHARACTER	TF34 BASE	TF34 IEG/S
Engine Pitching, Rear Mount	2008	2005
Engine Transmission	2653	2594
Fan Case/Fan Transmission	3932	3945
PT/HPT Transmission	4697	4661
HPT/Casings Transmission	5456	5401
Compressor Case Bending/ HPT/Fan Case Transmission	6560	6538
Fan/Compressor Case Transmission	8837	8784
Stators/(Int. Starter)	11876	10169
LPT Shaft Bending	12264	11631

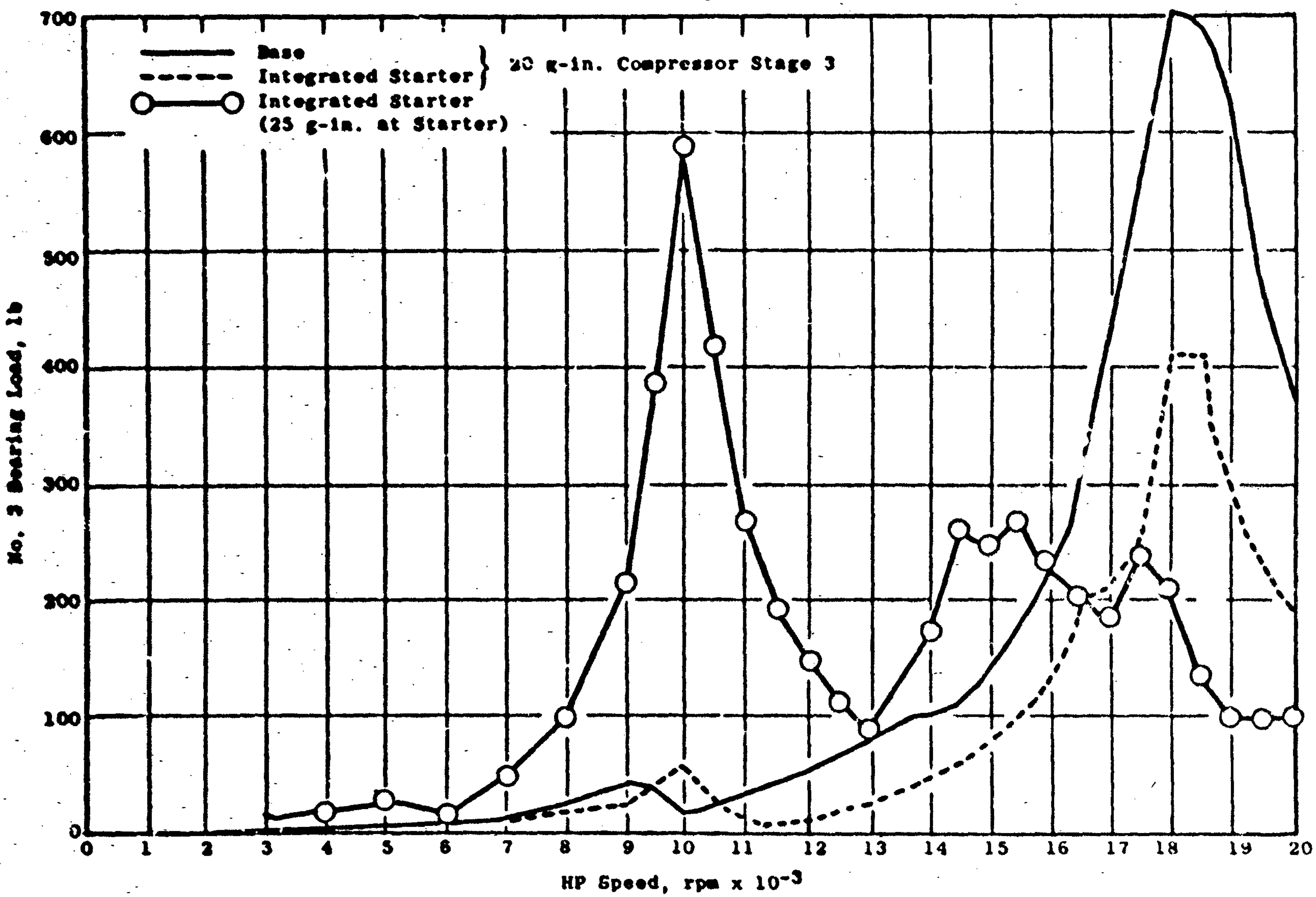

Figure 46. TF34 No. 3 Bearing Load, Compressor/Starter Unbalance.

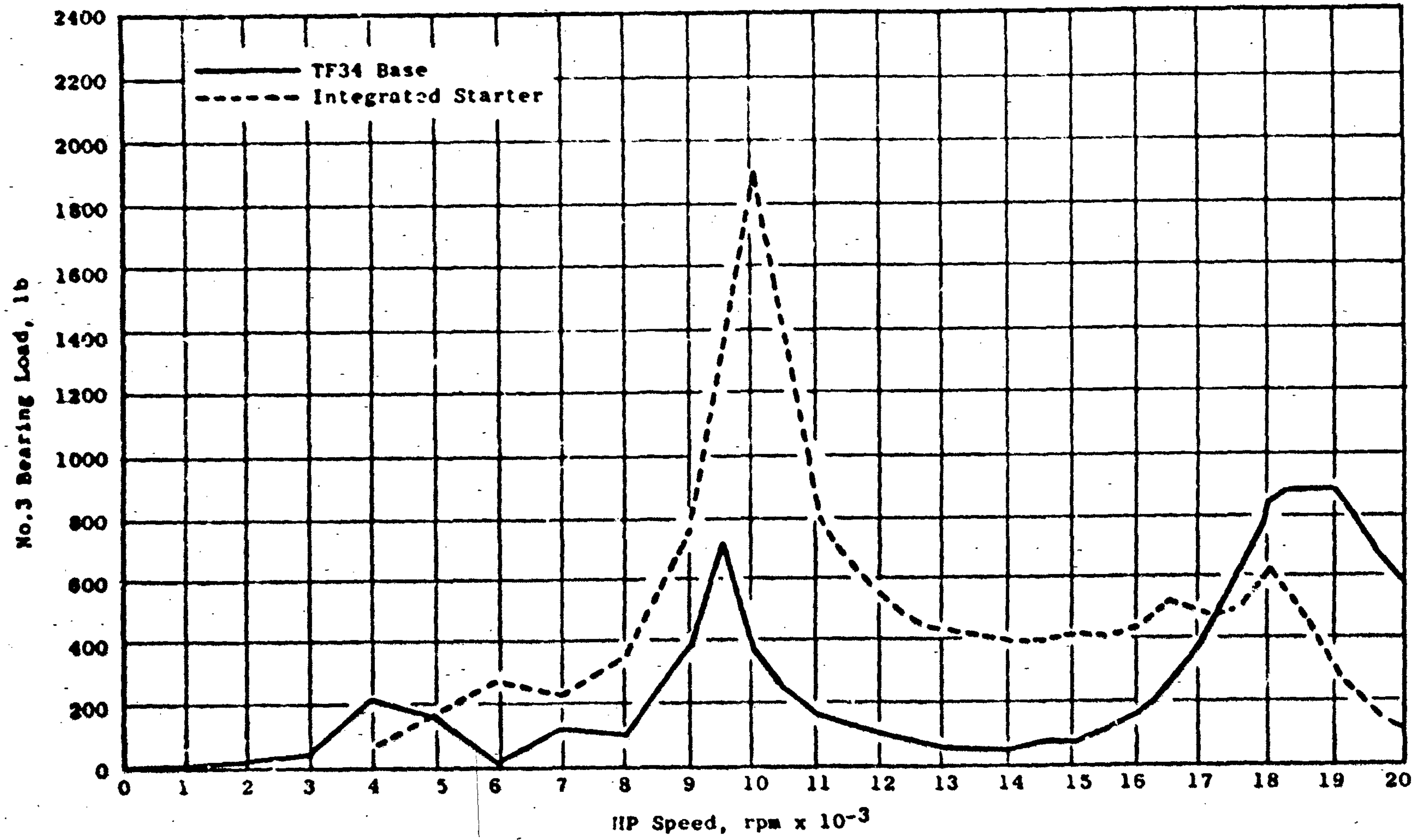

Figure 47. TF34 No. 3 Bearing Load, HPT Unbalance.

- Since the No. 3 bearing loads are also much higher at idle for HPT unbalance, either better HPT balancing or a higher gas generator idle speed will have to be considered.

5. Effect of Engine Dynamics Upon PM Machine Radial Air Gap

The relative radial deflection of the generator rotor in relation to the generator stator has been investigated. The study assumed a soft No. 5 bearing with the No. 2 and No. 3 bearing stiffness characteristic unchanged from the baseline engine.

a. Results

Figure 48 is a plot of the IEG/S generator clearance changes for 115 gram-inches at HP-turbine Stage 2, 20 gram-inches at the compressor Stage 3, and 25 gram-inches at the IEG/S rotor.

b. Conclusions

The IEG/S radial air gap change is most significantly affected by HPT unbalance and to a lesser extent by unbalance at the IEG/S rotor.

c. Recommendations

The HPT and IEG/S-rotor will need to be well balanced to maintain acceptable clearance changes for the IEG/S.

6. Containment of Failed Rotor Parts

In the event of a rotor shrink ring failure, the rotor parts - shrink ring, magnets, and pole pieces - are to be retained within the forward sump area. Two principal ways of establishing the necessary requirements can be considered:

1. The outer containment shell can be made strong enough to contain the impact of the rotor pieces. In order to achieve this, the outer shell must be designed to withstand twice the pressure level these parts create at maximum overspeed.
2. The total energy in the rotor to be absorbed must be limited to the plastic deformation of the containment shell without rupturing this shell.

In the first approach the radial pressure level of the rotor parts is reflected in the hoop stress of the retaining ring. The stress level in this

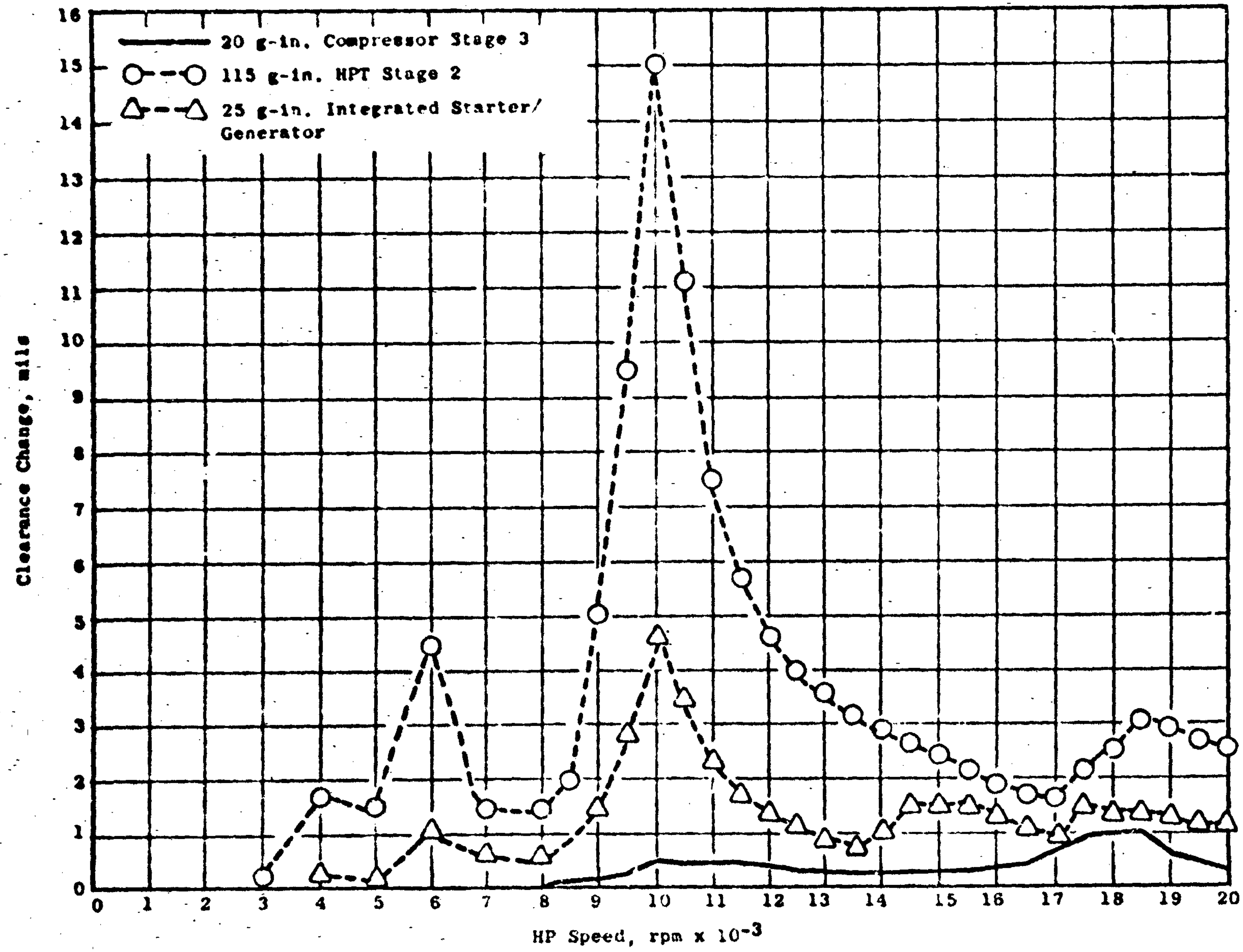

Figure 48. Dynamic Effect of TF34-IEG/S Air Gap Clearance Change.

ring is already so high that either a stationary retaining ring of the same thickness would have to have 300,000 psi stress capability or else a stationary retaining ring of the nominal stress capability would have to be twice as thick. This capability would require an impractically high weight.

The conditions in the second approach are best illustrated by an example. Taking the rotor dimensions and maximum overspeed for the 120 KVA PM generator for the TF34 application in its final version, one calculates for a ring of 4.67-inch axial length, 3.9-inch outer radius (R_o), and 1.[illegible]-inch radius (R_i) a total energy of

$$E = 228{,}600 \text{ ft-lb}$$

If one considers the magnetic steel in the stator plus the cooling shroud and a 0.2-inch-thick aluminum frame without the copper windings, the energy levels necessary to plastically deform these parts (up to tensile strength) are found to be as follows:

Permendur Stator Core	Ed = 28,340 ft-lb
Medium-Carbon Steel Cooling Shroud	Ed = 15,660 ft-lb
Aluminum Frame (XA201.0-T7)	Ed = 14,030 ft-lb

for a total deformation energy of

$$Ed = 58{,}030 \text{ ft-lb}$$

This is a quarter of the total rotor energy. Thus if all the disks on the rotor were to fail at the same time, it appears that an additional containment wrapper would be needed, such as the kind used in aircraft engines to retain broken-off blades. (The F103 uses a Kevlar wrapper for the fan blade section.)

It is extremely unlikely, however, that a rotor built from multiple disks will have all its disks fail at once. More likely, the disks would fail one at a time. In the case of four disks the stator seems to have sufficient energy absorption capability (through deformation) to contain this one disk.

Presently available analysis methods make it extremely difficult to accurately analyze the subsequent disk failures and their containment. It is not sufficiently understood how much energy absorption capability the stator materials have if they are exposed to a dynamic deformation process over an extremely short time span. In order to describe what happens in a subsequent disk failure one needs to define the state of the machine after the first failure.

Presently, whenever these kinds of problems occur the development of the selected method of containment is based on extensive testing. (The containment wrapping of the four stages for the F103 engine has been developed that way.) General Electric Company Corporate Research and Development is presently developing containment structures for multidisk flywheels utilized for flywheel energy storage applications. It is therefore suggested that the containment approach be the subject of a special program which investigates the failure behavior of such a rotor and develops the proper containment strategy. Based upon present experience and understanding of this rotor failure mode, it is not expected that the containment scheme necessary for protection will add weight and require more space to a degree that would significantly change the conclusions of this study.

7. Description of Interface Between Selected Starter/Generator and Engine

a. Starter/Generator Description

The following starter/generator description is based upon the starter/generator developed under Air Force Contract F33615-74-C-2037, and can be considered as scaled down from the contract 150 KVA machine to this machine which has a 120 KVA rating. Thus, the concepts in design and manufacturing techniques have been established from test and/or operational experience with rare earth/cobalt permanent magnet Variable Speed Constant Frequency (VSCF) starter-generators.

In the following sections the salient features and characteristics of the starter/generator are described, along with the general configuration and function of its major components, so that the selected configuration can be better understood.

b. Starter/Generator Overview

The starter/generator system rated at 120 KVA is a permanent-magnet radial air-gap machine with nine-phase output winding and a permanent field provided by rare earth magnets that are contained in an all-metallic rotor.

The starting capability is provided by application of power from the converter to the generator output windings. The generator will therefore operate as a brushless d.c. motor. Sensors are used to detect angular relationship between the rotor poles and phase windings, functioning as the commutator, such that power can be applied to the proper phase to achieve the required engine starting torque.

The generated voltage and power output to the converter are functions of speed. Therefore, the generator is designed to be capable of delivering rated load and meeting overload requirements at the base speed. At higher speeds the generator has the capability of delivering power exceeding specification requirements.

The winding is cooled by oil circulated in discrete channels around the stator frame.

This type of starter/generator is considered inherently more reliable than conventional, wound-rotor-type a.c. generators since the generator does not contain rotating windings, eliminates the use of rotating rectifiers, has but one output winding, and is simplified by using substantially fewer parts.

A layout cross-sectional view of this starter/generator, with identification of components as referenced and described herein, is shown in Figure 49. Table 60 contains a comparison between the tradeoff design and the detail design made for the selected Level III system. The designs are different due to refinements in the magnet requirement which resulted in a six percent decrease in stack length. Additional machine details are shown in Table 61.

Rotor

The rotor is an 18-pole ring segment design with four ring segments 1.16 inches long and 7.80 inches in diameter. Each segment is constructed to contain the permanent magnets and the metallic members and to provide the required magnetic path and mechanical strength. The ring segments are aligned and assembled onto a shaft sleeve and held in place with a shrink collar. The parts of the rotor (Figure 50) are further described as follows:

Shaft Sleeve - The shaft sleeve would be made from a nonmagnetic, high strength material such as Inconel 750, to prevent interference with the magnetic flux path. This part would form the interface to the engine shaft and allow the four rotor disks to be assembled prior to their assembly into the engine.

Slingers - Located on both ends of the shaft sleeves are slingers which serve to minimize the amount of oil that contacts the rotor. These slingers would be made of a high strength nonmagnetic material. One slinger is a separate part and would also serve as a shrink collar to hold the rotor disk onto the shaft sleeve. The other slinger is integral with the shaft sleeve, provides proper angular alignment of the 4-hub segment, and provides a positive anti-rotation

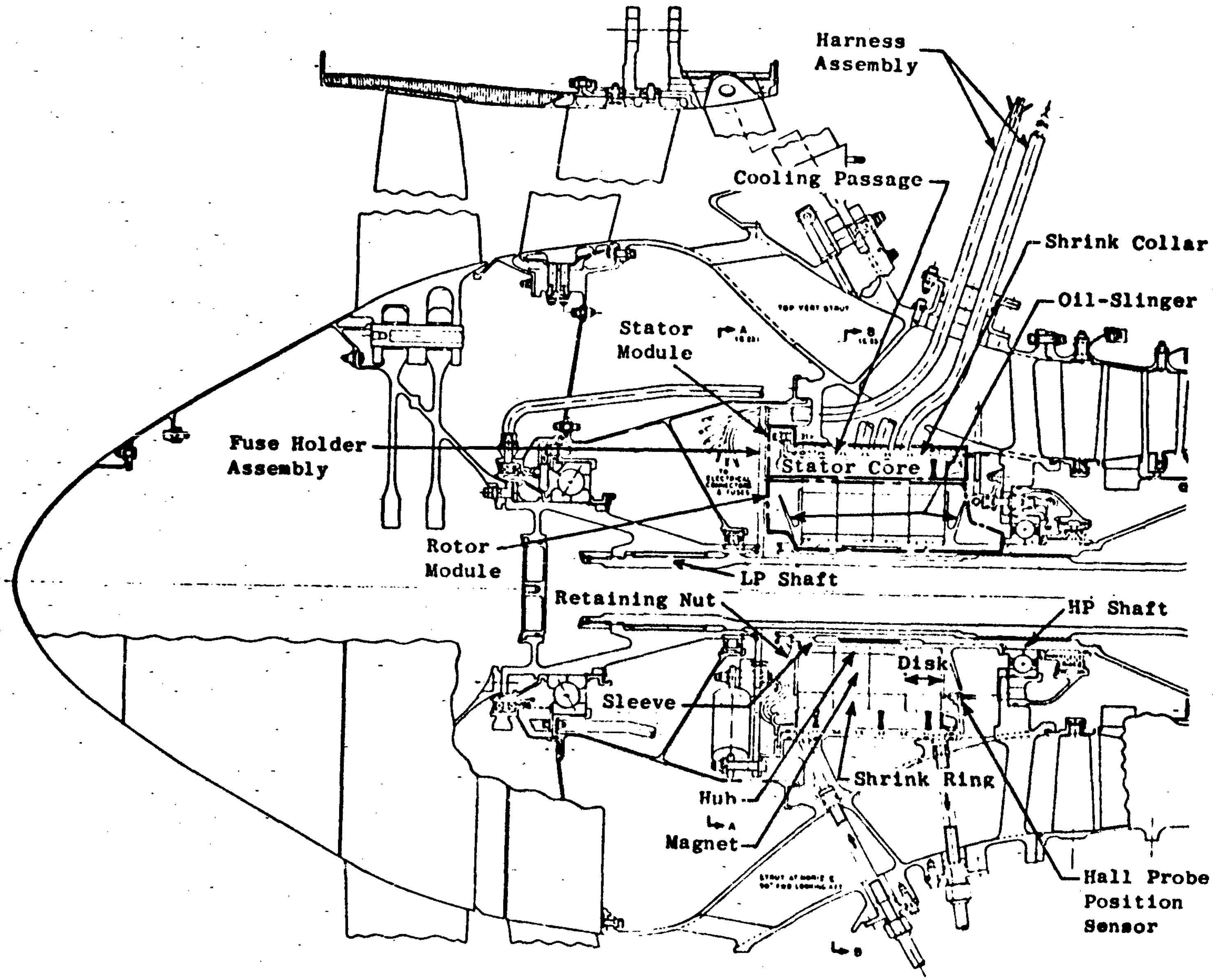

Figure 49. High-Pressure-Shaft-Mounted IEG/S Permanent Magnet Machine for TF34.

TABLE 60

FINE TUNING OF 120 KVA MACHINE DESIGN

DIMENSION	TRADEOFF	DETAIL DESIGN
Rotor Diameter	8.0	7.800 in.
Stator Base Diameter	8.112	7.934 in.
Punching Diameter	9.214	9.142 in.
Magnet Height	1.210	1.373 in.
Magnet Length	0.607	0.591 in.
Shrink Ring Thickness	0.520	0.510 in.
Stack Length	4.91	4.633 in.
Length Over End Turns	6.03	5.722 in.
Maximum Length	6.50	6.20 in.
Volume (electromagnetic)	473	407 $in.^3$
Weight (electromagnetic)	75	75 lb
Losses	9.32	9.1 kW (2 p.u. load)

TABLE 61

FINAL MACHINE DATA FOR 120 KVA GENERATOR FOR TF34 POWER LEVEL III APPLICATION

Pole Pairs	9
Base Frequency	1407
Phases	9
Turns/Phase	12
Per Unit Pole Arc	0.5
Gap Flux Density	52 KL/in.^{2}
Number of Slots	108
Stator Bore Diameter	7.934 in.
Slot Width	0.135 in.
Slot Depth	0.426 in.
Slot Opening	0.070 in.
Stack Length	4.633 in.
Length Over End Turns	5.722 in.
Yoke Thickness	0.179 in.
Punching Diameter	9.142 in.
Rotor Diameter	7.800 in.
Shrink Ring Thickness	0.510 in.
Magnet Height	1.373 in.
Magnet Length	0.591 in.
I^2R Losses at 2 p.u. Load	4.7 kW
Iron Losses at 2 p.u. Load	4.5 kW
Weight Total Electromagnet	75 lb
Rotor Weight	50 lb

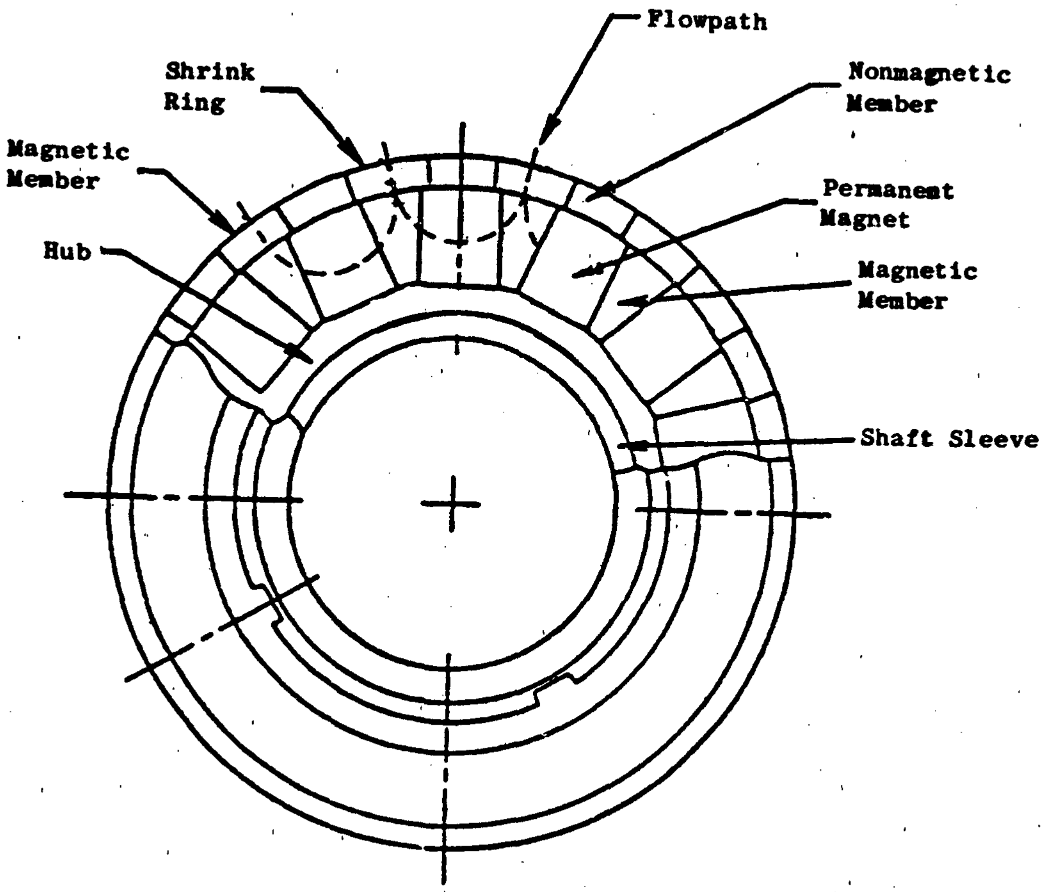

Figure 50. Rotor Cross-Sectional View.

feature. Torque, however, is assumed to be transmitted by friction (shrink-fit).

Hub - The hub would also be made from a nonmagnetic high strength material, such as Inconel 750, to prevent shorting the magnets out at the inside diameter. The hub provides the base or support for attachment of the spoke-configured magneti members and the permanent magnets.

Magnet - The design is based on using a rare earth/cobalt permanent magnet with an energy product of not less than 21 x 10^6 (B_d, remanent induction x H_d, demagnetization force) measured at room temperature. The magnets are magnetically oriented in the tangential direction to provide a "flux squeezing" effect which increases both effective pole flux and, in turn, the air gap flux density.

The magnet size is 1.373 inches in the radial direction as measured in the center and 0.591 inch wide.

Magnetic Member (Spoke-Configured) - The spoke-configured magnetic member is fabricated from low carbon steel such as 1010 steel and attached to the hub by electron-beam weldments. This forms the magnetic pole and provides the necessary flux path to the air gap.

Slot Design - The machine design was optimized in the slot opening/air gap geometry. The original study was done with open slots, an approach which has been discussed periodically. Open slots cause high eddy current losses in the rotor pole preface. The advantage of open slots is their low slot leakage reactance. Finite element analysis methods were used to determine slot leakage and slot harmonics effects, and working from these findings the ratio of air gap length to slot opening was balanced in order to minimize rotor surface losses while keeping the overall commutating reactance constant. This was achieved by compensating for an increase in leakage reactance by a decrease in synchronous reactance. The calculations indicate that by reducing the slot openings from 0.135 inch to 0.070 inch (air gap length), the eddy current losses at full-load top speed would be only 300 watts.

Shrink Ring - The shrink ring provides the necessary support for the magnets and pole pieces through the overspeed rating. It is fabricated from alternating sections of magnetic and nonmagnetic materials that are welded by electron beam process to form a ring. The use of magnetic material in the ring allows the effective magnetic air gap to be significantly reduced and in turn to minimize the magnet size. Typical shrink ring material would be MP35N

(nonmagnetic) and Ni-Mark 300 (magnetic). The shrink ring radial thickness for this machine is 0.510 inch.

Rotor Assembly - The finished rotor has an outside diameter of 7.800 inches and a stack length of 4.633 inches.

Stator - The stator is similar in construction to other conventional a.c. machines. It is constructed with a wound laminated magnetic core and an outer aluminum shroud of high-strength aluminum alloy. The laminated core is 9.12 inches in outside diameter and 4.633 inches in length, has 108 slots, and contains a nine-phase, multiple-strand, round conductor winding. (See Figure 51 for generator schematic.) The parts of the stator are further described as follows:

Stator Core - The stator core material selected was 0.006-inch-thick vanadium-cobalt-iron, an alloy commonly known as "Vanadium Permendur." This alloy, when properly processed, permits design for operation at substantially higher flux densities with lower magnetizing current than conventional magnetic steels, thereby permitting an electromagnetic design that will result in a smaller and lighter machine. The 0.006 inch thickness was selected to minimize eddy current losses at the high operating frequencies.

The laminations are stacked and aligned, and secured by bonding. The phase windings would be insulated from the laminated core slots with a double-liner polyimide film. Two thicknesses are used to assure dielectric reliability at the voltages generated at the higher end of the speed range. Different phase windings occupying the same slots are insulated from each other with a polyimide film member and coils mechanically secured using an interference-fit, rigid polyimide top stick.

Phase Winding - The phase coils are round copper conductors, which are enameled with quadruple-build polyimide to enhance phase-to-phase and phase-to-core insulation reliability.

The coil turns are wound in strands, or in multiple, to reduce "skin effect" I^2R losses. The strands are transposed in the end turns to cancel out the strand-to-strand voltages generated in the slot and thereby minimize the "deep bar" I^2R losses.

The sizing of conductors in design is limited by a current density of 7,750 amperes per square inch at rated load.

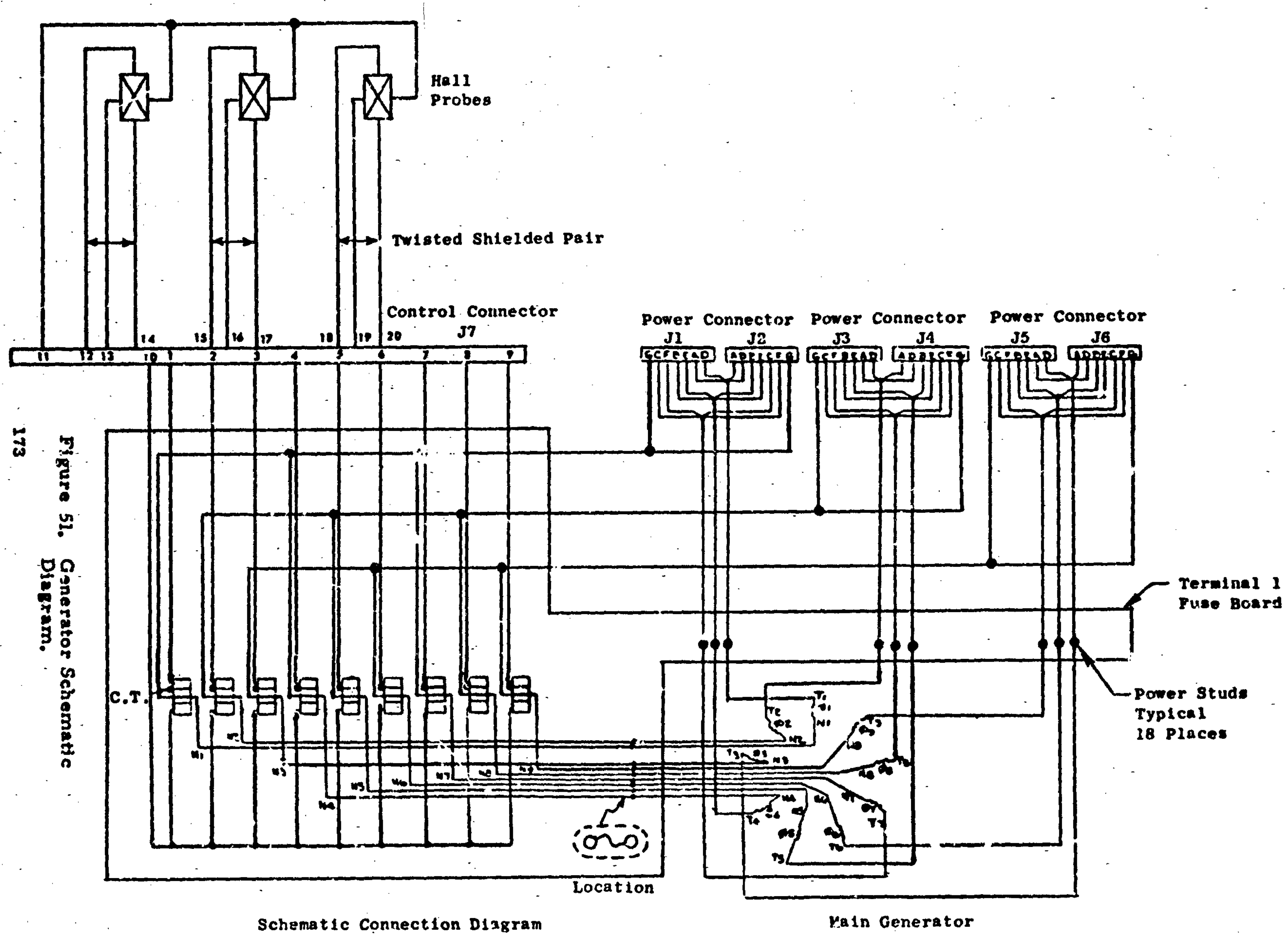

Figure 51. Generator Schematic Diagram.

Laminated-End - Two melamine glass laminations of 0.062 inch thickness are used on each end of the stack to protect the phase winding entry and exit of the slot area against cut-through on the steel lamination corners. This protection will increase the winding reliability.

End Turns - The phase winding end turns are formed back from the bore and against the insulated extended aluminum shroud member to provide close contact with the thermal circuit for oil cooling. This arrangement provides important cooling of the phase windings and supplements the transfer of heat from the slots. The phases are additionally insulated from each other in the end turns using 0.003-inch polyimide film insulators.

Connections and Leads - The terminations of phase windings are brazed with connections of each phase made to a multiple-wrap-polyimide-film-insulated stranded copper cable consisting of four AWG No. 10 wires in parallel that have a terminal that connects to the machine terminal board.

Winding Impregnation - The stator winding would be impregnated with multiple vacuum-pressure processing. This compound and process assures maximum slot fill and coil bond to effect best transfer of heat from the slot and end turns into the core, aluminum shroud, and cooling oil in the frame.

Winding Support - As shown in Figure 52, support would be used to help support the winding end turns and lead connections as well as provide protection when the rotor might strike the end turns during assembly.

8. Other Electrical System Components

The following section provides the functional description of the remaining parts, other than the rotor and stator, associated with the starter/generator.

a. Protection Device (Disconnect)

As discussed previously, the protection device selected is fuses. The fuse selected is a rectifier-type fuse with the following characteristics:

Size:

Fuse Diameter	1-7/32 inches
Fuse Length	2-1/8 inches
Blade Length	3-5/8 inches

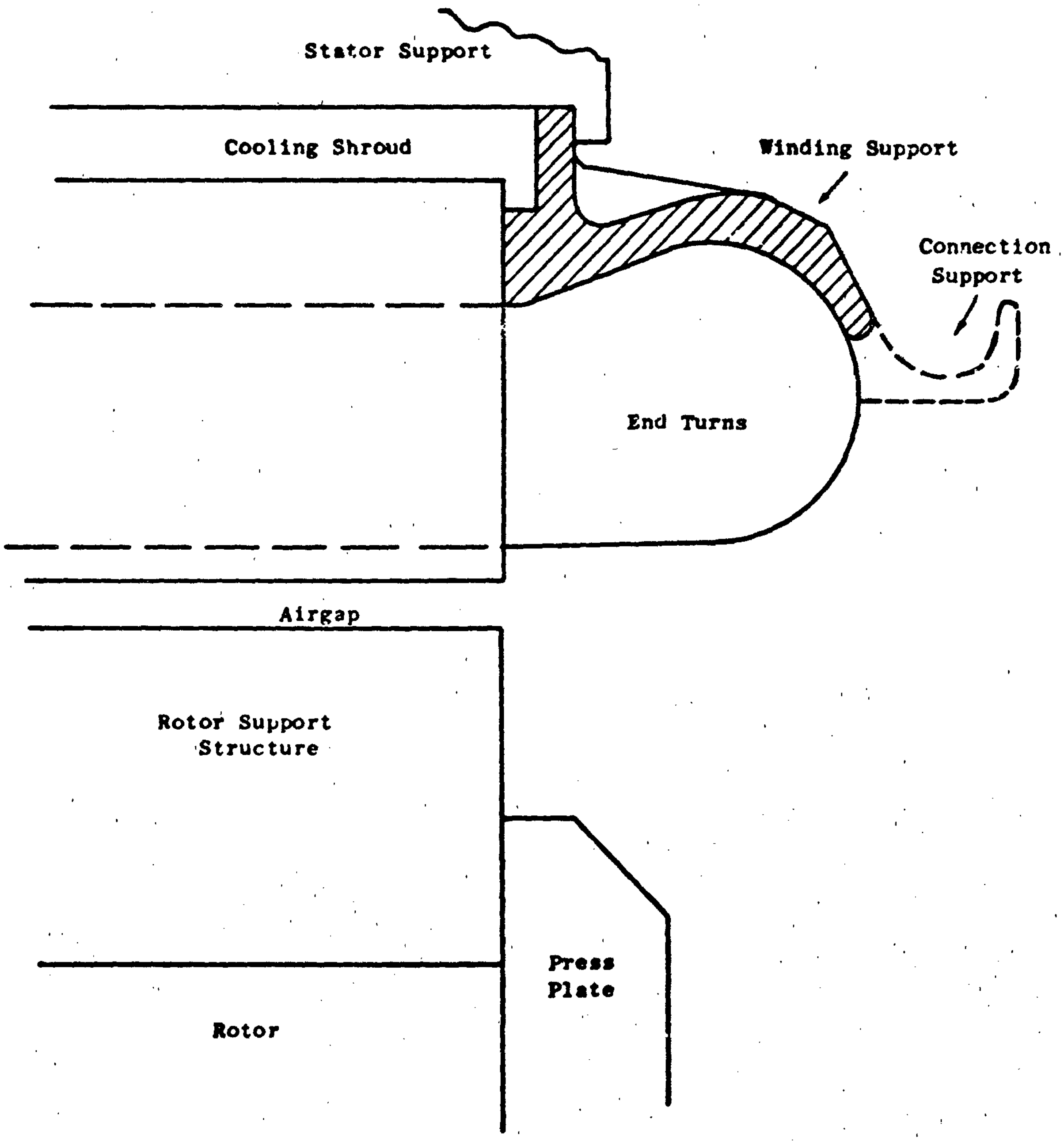

Figure 52. Conceptual Sketch of PM Stator Winding Support.

Rating:

1 p.u.	Continuous
1.5 p.u.	5 minutes
2.0 p.u.	60 seconds
3.0 p.u.	5 seconds

See generator schematic (Figure 51) for wiring and location details.

b. Power Connections

The stator output windings are terminated in threaded power studs. There are nine neutral and nine power leads, each consisting of four AWG No. 10 insulated leads in parallel, terminating into a threaded power stud. Each power stud is inserted into the terminal/fuse board located on the forward section of the generator/engine interface to form the power connection interface.

c. Performance Monitors

The generator would be equipped with current unbalance and speed sensor performance monitors. These monitors are functionally described as follows:

Current Unbalance - A multiturn, single-coil current transformer is positioned around each phase neutral lead. The transformers will be located on the terminal/fuse board. These current transformers will provide a signal to the converter with indications of phase current unbalance and high frequency feeder cable fault.

Speed Sensor - Three Hall-effect elements will be used to provide rotor position and rotor speed sensing during starting. Each of these elements provides an output signal as a function of magnetic flux. They are located 120 electrical degrees apart on the aft stator frame, and are positioned so as to detect the pole leakage flux of the permanent magnet generator.

A small sensor core of laminated silicon iron laminations is utilized to increase the magnetic flux through the Hall element. The leads of the Hall-effect device are covered by a shield to eliminate noise signals that might be generated in this signal device. The shielding is terminated adjacent to the control connector, and the Hall generator leads are terminated in the control connector.

Terminal/Fuse Board - Located at the forward end of the generator is a large, nonmetallic, donut-shaped plate which is mounted directly above the generator end turns. This plate serves as a junction area for the generator leads and high frequency cables as well as the mounting area for the nine current transformers and nine fuses. This plate would be made from Vespel, a high-strength, nonmetallic material with excellent thermal characteristics.

High Frequency Cables - Exiting the terminal/fuse board would be a cable with terminal lugs on one end and connectors on the other. This arrangement will allow the generator to be removed from the engine without pulling the cables. These cables would be spliced into a harness assembly and sealed by a removable plug on the outside of the engine. High frequency cables with connectors on both ends will be routed to the remote-located converter. The high frequency cable within the engine and outside the engine has the following characteristics:

Number of cables	6
Wires per cable	7
Wire size	10 AWG
Current per conductor @ 1 p.u.	29.5 amps
Cable cover	shield 97 percent min
Jacket	Teflon
Diameter	0.486 inch max
Weight	0.277 lb/ft
Bend radius	4.86 inches min

9. IEG/S Losses

a. Generator Losses

The calculated generator losses are shown in Table 62.

b. System Losses

The system losses consist of the generator losses plus converter losses. They are shown in Table 63.

10. Thermal Insulation Life Prediction

A thermal analysis using a three-dimensional mesh network was applied to the rotor and stator to establish the winding hot-spot temperature. The winding end turns were found to be 160° C at full load, top speed with an 80° C oil

TABLE 62

CALCULATED GENERATOR LOSSES

Generator Losses (kW) at Base Speed

System Load	1 p.u.	3/4 p.u.	1/2 p.u.	1/4 p.u.
Windage	1.5	1.5	1.5	1.5
Pole Face	0.2	0.2	0.2	0.2
Core	1.8	1.8	1.8	1.8
I^2R	1.5	0.8	0.4	0.3
	5.0	4.3	3.9	3.8

Generator Losses (kW) at Top Speed

System Load	1 p.u.	3/4 p.u.	1/2 p.u.	1/4 p.u.
Windage	2.3	2.3	2.3	2.3
Pole Face	0.6	0.6	0.6	0.6
Core	4.2	4.2	4.2	4.2
I^2R	1.5	0.8	0.4	0.3
	8.6	7.9	7.5	7.4

TABLE 63

SYSTEM LOSSES

1 per unit (p.u.) system load = 120 KVA

System Losses (kW) at Idle Speed

System Load	1 p.u.		3/4 p.u.		1/2 p.u.		1/4 p.u.	
	kW	η%	kW	η%	kW	η%	kW	η%
Converter	5.9	95	4.8	95	3.7	95	2.6	94
Generator	5.0	96	4.3	95	3.9	94	3.8	88
Total Losses	10.9		9.1		7.6		6.4	
Output at 0.95 PF	114.0		85.5		57.0		28.5	
Power Input	124.9		94.6		64.6		34.9	
System Efficiency, Percent	91.3		90.4		88.2		81.7	

System Losses (kW) at Takeoff Speed

System Load	1. p.u.		3/4 p.u.		1/2 p.u.		1/4 p.u.	
	kW	η%	kW	η%	kW	η%	kW	η%
Converter	8.6	93	7.5	93	6.5	90	5.5	85
Generator	8.6	93	7.9	92	7.5	88	7.4	79
Total Losses	17.2		15.4		14.0		12.9	
Output at 0.95 PF	114.0		85.5		57.0		28.5	
Power Input	131.2		100.9		71.0		41.4	
System Efficiency, Percent	86.9		84.7		80.3		68.9	

inlet temperature. This was for an operating schedule of:

91.55 percent of time at less than 82 percent speed (100 percent = 17,500 rpm) and full load

7.6 percent at 100 percent speed and full load

0.8 percent at 1.5 per unit load and 100 percent speed

0.05 percent at 2 per unit load and 100 percent speed

The above hot-spot temperature gives an insulation life prediction of greater than 180,000 hours. This is based upon life expectancy data for insulation systems used on similar aircraft generators. The current density at full load for this machine is 8500 A/in.^{2}

11. Generator Reliability Assessment

A detailed reliability study was not conducted on the selected machine because of its similarity to the rotor construction used on conventional a.c. synchronous aircraft permanent magnet control generators (multiple pole with a shrink ring). The following reliability study is based on taking the reliability analysis of a permanent magnet control alternator and adjusting it for the number of poles and the additional length to attain the stack length. The stator analysis was obtained by taking a similar main stator wound and removing those components not being used. The result of this reliability assessment is shown in Table 64.

12. Forces Due to Rotor Misalignment

Additional inputs were required for the mechanical interface design to assess the electromagnetic forces that were due to rotor eccentricities.

For a cylindrical PM machine, these forces are derived from the following equation. The PM-type cylindrical design is characterized by a synchronous reactance in the direct axis, which is generally less than the synchronous reactance in the quadrature axis. This is contrary to electrically excited synchronous machines and will also be reflected in the force equation.

For the unsaturated machine at no-load one can arrive at the eccentricity force equation listed below.

$$F = \frac{DR \cdot HI}{2\pi \cdot \mu o} \Delta \hat{B}_g^2 \qquad (13)$$

TABLE 64

RELIABILITY ASSESSMENT
FOR
INTEGRATED TF34 ENGINE STARTER/GENERATOR
POWER LEVEL III (120 KVA SYSTEM)
CYLINDRICAL PERMANENT MAGNET MACHINE

	PART FAILURES/10^6 HOURS	
ROTOR		
Magnets *	4.49896	
Weld Joints *	6.74844	
Shaft Internal Fit	1.20188	
Keyway	0.15360	
Key	0.35260	
Wavy Washer	0.2000	
	13.15548	13.15548
STATOR		
Stator Coil	0.47709	
Elec. Conn.	0.16737	
Interference Fit	0.30047	
O-Rings	1.59400	
Attachment Holes	3.02204	
	5.56097	5.56097
		18.71645

Total MTBF Hours: 53,428.94

* Assumes four (4) rotor disks to make total stack length.

$$\text{or:}\quad F = \frac{DR \cdot HI}{2\pi \cdot \mu o} \cdot \left[\frac{Br \cdot hm \quad P_{LE} \cdot \delta_{OE}}{\left(\frac{K_{FI}}{K_m} \cdot \frac{\tau_p}{4} + \delta_{OE} \cdot P_{LE} \right)^2} \right]^2 \cdot \epsilon^2 \qquad (14)$$

With DR = rotor diameter

- HI = stack length
- Br = remanence flux density of magnet
- P_{LE} = total magnet leakage permeance
- δ_{OE} = effective design air gap
- K_{FI} = yoke flux constant
- K_m = gap flux constant
- T_p = pole pitch
- ε = Δ gap/effective design gap
- ΔB_g = difference in air gap flux density between two points in the air gap 180° apart.

For the PM machine selected the resulting force is

$$F = 538 \times \varepsilon^2 \text{ (lb),}$$

where the effective air gap is 0.071 inch. However, saturation will significantly reduce this force by increasing the magnetically effective air-gap length. For the above example, the magnetically effective air gap becomes

$$\delta_{SNLE} = 0.180 \text{ in.}$$

Table 65 illustrates the effect of saturation for no load and full load. The full load point also takes into account the effect of armature reaction, as indicated. In total, no significant eccentricity forces are expected.

C. COMPARISON OF SELECTED DESIGN WITH BASELINE TF34

These comparisons do not include total impact on the aircraft, which is beyond the scope of this study. The impact of replacing hydraulic lines and actuators with the larger-sized electrical cables and the electric motors is not included. These are a part of the subject being addressed in studies of an all-electric aircraft. More accurate comparisons do require consideration of the impact on the total aircraft and would require the participation of an air framer in the study. Figure 53 shows the baseline TF34 engine with conventional gearbox-mounted accessories. Earlier, Figure 44 showed the selected IEG/s integrated into the TF34 engine. A comparison of major design characteristics pertaining

TABLE 65

ECCENTRICITY-INDUCED FORCE VS. SATURATION

Geometrical air gap = 0.067 inch

Eccentricity = 0.022 inch (33 percent)

Load	Gap., In.	ε, %	F, lb
No Load unsaturated	0.071	0.27	39.2
No load saturated	0.180	0.111	3.0
Full load saturated	0.085	0.235	13.6 *

*Also includes effect of reduction of air gap flux density by armature reaction.

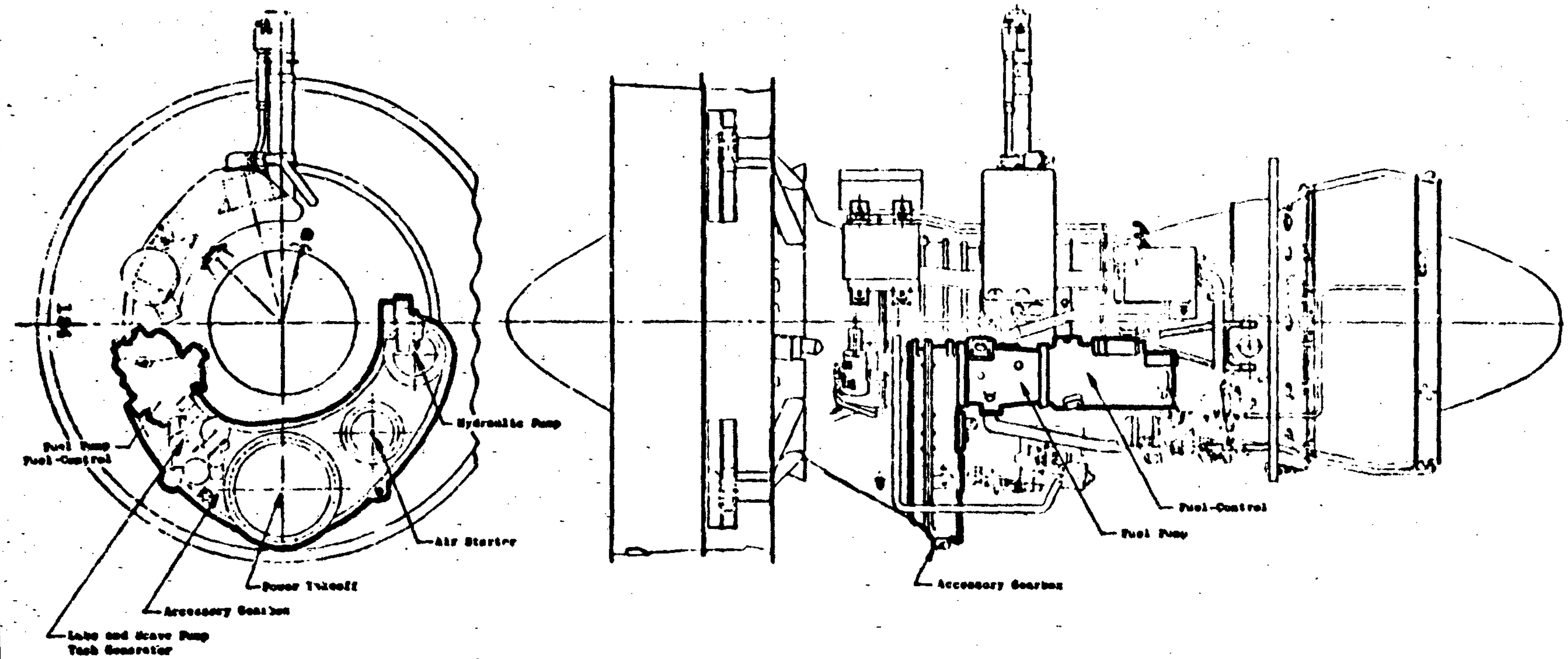

Figure 53. Baseline TF34-100 Layout.

to the differences between baseline and IEG/S configuration is discussed below.

1. Weight

Weights are either actual TF34 component weights or calculated from preliminary design layout drawings. Table 66 gives a weight comparison between the baseline and IEG/S configuration.

2. Reliability

The PM machine IEG/S design derives its superior reliability through less parts and the elimination of mechanical wear components in the electrical secondary power extraction system.

A comparison of reliabilities between baseline and IEG/S system is given in Table 67.

3. Efficiency

Table 68 presents efficiences that reflect the present power extraction on the TF34 as compared to the anticipated TF34 engine with an IEG/S. Figure 54 shows conventional IDG heat rejection.

4. Initial Cost

Initial costs of major components are listed in Table 69 for the baseline and the IEG/S system. Costs are based on a production lot of 250 units and 1979 dollars.

5. Comparison Summary

Table 70 provides an overall comparison between the selected IEG/S-TF34 and the baseline TF34 system.

TABLE 66
WEIGHT SUMMARY - BASELINE AND IEG/S FOR A TF34

Baseline - TF34		IEG/S - TF34	
Item	Weight (lb)	Item	Weight (lb)
Mech. Drive System	134.0	120 KVA SmCo Gen. Start.	108.0
30/40 KVA IDG	78.0	120 KVA Cyclo Converter	96.0
Air Turbine Starter	22.0	Additional Eng. Structure	10.0
Air Start Valve	7.0	Additional Eng. Shaft	5.0
Start - Air Duct	4.0	2-ft power cables	3.5
Bleed - Air Duct	5.0	Electric Disconnect Assy.	10.0
LDE heat exchanger and hoses	8.0	10 kW Motor for Fuel/Lube Pump (Force Commutator PM brushless motor and electronics)	34.0
Total	258.0 lb		267.0 lb

TABLE 67. RELIABILITY SUMMARY - BASELINE AND IEG/S FOR A TF34.

Baseline - TF34		IEG/S - TF34	
Item	MTBF (Hours)	Item	MTBF (Hours)
Mech. Drive System(1)	34,000	PM cyl. Machine/Disconnect(4)	50,000
30/40 KVA IDG El. System(2)	979	Cycloconverter	10,000
Air Turbine Start System(3)	471	Electric PM Motor for Fuel/Lube Pump Electronics(5)	10,000
System MTBF (Hours)	315	System MTBF (Hours)	4545

Source: (1) GE-AEG data bank
(2) AFM 66-1 data 6 mo. Ave. ending 30 April 1979
(3) AFM 66-1 data 6 mo. Ave. ending 31 January 1978
(4) Calculated reliability (see Section VII-B-11)
(5) Estimated

TABLE 68
EFFICIENCY COMPARISON BETWEEN
BASELINE TF34 AND IEG/S TF34

	30/40 KVA IDG		120 KVA IEG/S	
	Idle	T/O	Idle	T/O
Accessory Gearbox Loss, 75 kW, η = 0.97	2.3	2.3	-	-
Electrical System Loss, kW	16.6 *	19.8 *	10.9 **	17.2 **
Pump Motor and Electronics Loss, kW 14 kW; η_{OA} = 0.88	-	-	1.1	1.7
TOTAL	18.9	22.1	12.0	18.9
System Efficiency	0.80	0.77	0.90	0.86

$\eta = \frac{\text{Power-Out}}{\text{Power-In}}$

* Based on actual IDG system data on 40 KVA 0.9 PF per Figure 54. (Reference: manufacturer's test report ATR-1186 Figure 5, Appendix I.)

** Based on 120 KVA, 0.90 PF and a system efficiency of 89.3 percent at idle speed and 83.7 percent at takeoff speed. (Reference: calculated data from Table 63.

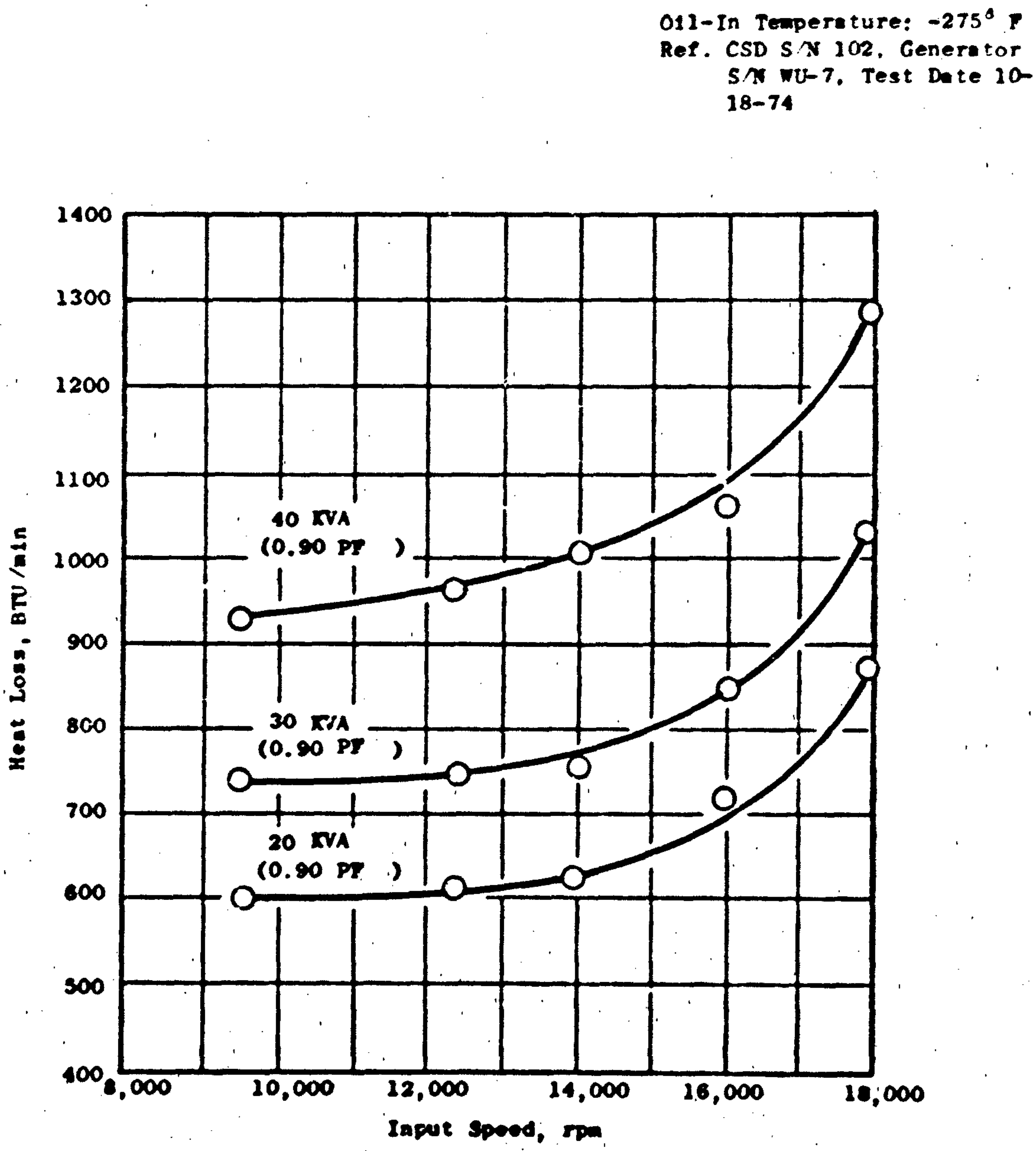

Figure 84. TF34/IDG Heat Rejection.

TABLE 69

COST COMPARISON

Baseline		120 KVA - IEG/S	
30/40 KVA System Generator	$4723	Cycloconverter	$31000
GCU	1410	IEG/S	22000
CSD	22250	El. Motor and Controls	5500
CSD Fuel/Oil Cooler	1550		
Frequency Control Unit	1000		
Control/Alternator	1000		
Air Starter	4600		
Air Starter Valve	1300		
PTO Gearbox	6300		
AGB Gearbox	21000		
Total	$65133		$58500

TABLE 70

SYSTEM PAYOFF COMPARISON BETWEEN

BASELINE TF34 AND SELECTED IEG/S TF34

	BASELINE TF34	SELECTED IEG/S TF34
Weight, lb	base	+ 9.0
SPS reliability, MTBF (hours)	315	4545
SPS Efficiency	80 to 75%	83 to 82%
Maintainability/ Accessibility	35 min./LRU replacement time	6 hours replacement time for IEG/S
SPS Overall System Efficiency	base	9% improvement
Frontal Area	base	same as base
Initial cost, for 250th unit, $	base	6.6K < base
Life Cycle Cost at 10 Years and 726-Aircraft Fleet, $	base	1/2 base

SECTION VIII

CONCLUSIONS AND RECOMMENDATIONS

The results of this study conclude that the SmCo, IEG/S concept is technically feasible and offers potential advantages over present secondary power generation systems in specific applications, specifically low bypass engines.

The primary potential advantages of the IEG/S concept compared to conventional aircraft systems are:

- higher reliability
- less maintenance
- lower life cycle cost

The overall effect and payoff of the IEG/S system versus a conventional system, however, cannot be determined by the engine designer or by the IEG/S system designer. It is therefore recommended that an evaluation of the advantages and disadvantages of the IEG/S systems be accomplished at the aircraft system level.

The proposed study program should establish the overall payoff and effect of the IEG/S system for advanced military aircraft with respect to mission requirements and economic considerations. This study would further include aircraft secondary power distribution, conversion, and utilization equipment for the following subsystems:

- Flight control, primary and backup
- Utility power (doors, landing gear and flaps, brakes, nose gear, steering, etc.)
- Lighting and instrumentation
- Environmental control
- Communication/navigation
- Anti-icing/de-icing
- APU starting (if applicable).

The potential advantages of the IEG/S is most significant for Power Levels IIIA and III for low bypass engines in high performance aircraft of near-sonic or supersonic operational capability.

The installation of the Power Level IIIA or III system eliminates the need for an AGB. The payoff from this elimination is a reduction in frontal area, especially in low bypass engines. The reduction in engine frontal area results consequently in a reduction of aerodynamic drag, thereby improving aircraft performance. Modern high bypass turbofan engines, however, can have the AGB mounted in the fan frame shadow, inboard of the fan bypass duct. This arrangement offsets the IEG/S advantage of engine frontal area reduction. The elimination of the accessory gearbox and the gearbox-mounted accessories, however, improves engine maintainability as well as simplifies the engine cowl and the nacelle configuration. The advantages of electrically driven engine accessories are that they can be freely located for ease of maintainability and they provide variable-speed operation capability. Pumps driven by variable-speed-controlled PM motors can meet varying flow demands independent of engine speed, thereby improving pumping efficiency. Improved pumping efficiency keeps the fuel temperature rise lower and increases the cooling capacity of the fuel for engine oil cooling.

The reliability of the IEG/S - including that of the safety disconnect inside the engine - presents a major consideration when assessing the feasibility of this concept. The selected IEG/S concept, which involves mounting the PM machine rotor directly onto the HP rotor shaft and using an electric (fused) nonresettable disconnect without moving parts, provides a prerequisite for attaining the required reliability goal of at least 50,000 hours MTBF. The study shows that the IEG/S, excluding the cycloconverter (external to the engine), has the potential for achieving an MTBF in excess of 50,000 hours.

The need for IEG/S accessibility inside the engine is less important because of:

- The absence of periodic maintenance requirement
- The absence of life-limited wear items
- High IEG/S system reliability

Therefore, replacement of the IEG/S and related components could be scheduled to coincide with normal engine maintenance.

The application of the IEG/S concept has its highest payoff potential for high-performance aircraft with low bypass engine(s), in which the frontal area of the engine installation (nacelle or fuselage) is influenced by the AGB package. The IEG/S concept in dual-spool, high bypass turbofans does not show a payoff

since there is no reduction in frontal area. However, the elements of this study extended to consider a gearbox, located inboard and aft of the fan case, driving a permanent magnet generator/starter. Such a gearbox would appear to have significant payoffs in terms of electrically driven components at the engine and airframe system level. This concept would constitute a lower development risk, providing all the advantages of an electrical secondary power system in addition to good access and without increasing the engine frontal area.

The applicability of the IEG/S to specific engines, therefore, must be studied for each individual engine.

A. ASSESSMENT OF ENGINEERING FACTORS DERIVED FROM PROGRAM STUDY

Reliability

The analysis has shown that the IEG/S permanent magnet machine mounted on the HP shaft can achieve 50,000 hours MTBF. This improvement in reliability over present aircraft generators is due to the use of conservative temperature and stress levels, a solid-rotor design, and the elimination of separate bearings, seals, and flexible drive splines (quill-shaft).

Life Cycle Cost

The projected life cycle cost of the IEG/S system is favorable in comparison to a conventional secondary power system. Since there is no maintenance requirement for the IEG/S (to coincide with engine maintenance), maintenance costs are reduced because of a dramatic reduction in the number of components. The entire mechanical accessory drive system (with all its bearings, seals, and gears) would be eliminated together with the pneumatic starter, starter valve, air ducting, IDG, control alternator (hydraulic pumps in the case of the all-electric aircraft), and gearbox mounting system, and the hundreds of components which make up the above assemblies.

Simplified System

The secondary power system components on the engine are reduced to:

- **PM machine/safety disconnect**
- **cycloconverter**
- **two electric motors and their control system**
- **fuel and lube pump.**

Flexibility of Engine Accessory Location and Operation

Strategic location of the engine accessories improves service tasks. Variable-speed operation of the engine fuel and lube pump provides more efficient operation. (Variable-speed fuel pumps deliver the exact amount of fuel required by the engine without bypass losses.)

Improved Maintainability of Engine

The elimination of the AGB and its related accessories, tubes, air ducts, and electric wires in a small space underneath the core cowl makes engine maintenance easier and less costly.

Improved Electric System Maintainability

The hydromechanical constant speed drive (CSD) would be replaced by the electronic cycloconverter. Unlike the CSD, the cycloconverter requires no maintenance and has no wearing parts. (Generator bearings, seals, and quill shafts are not needed.) The superior reliability of the IEG/S electric system makes it easier to maintain.

For the long-range prospects, electronic components will become even more reliable due to growth (the use of fewer parts) and improvements in a relatively new technology.

Efficiency

The TF34-IEG/S system power extraction efficiency shows an 11 percent advantage over the conventional system (based on Power Level III).

Weight

The weight of the 120 KVA PM generator is 108 lb or 0.90 lb/KVA. The IEG/S system weighs approximately the same as the conventional system. The Power Level III IEG/S system, however, will show a significant weight advantage at the aircraft level because of the elimination of bleed-air ducts, hydraulic lines, and associated equipment.

Engine Frontal Area

The engine frontal area of some conventional engines (particularly low bypass engines) is affected by the engine-mounted accessory gearbox and the accessories. High bypass engines have multiple choices for their AGB location. The two most common mounting locations are the fan case and the core. The engine

frontal area is affected only indirectly by the core-mounted AGB, to an extent determined by engine size, bypass ratio, and the relative size of accessories.

Engine performance not evaluated in this study effort, however, might be influenced by the fan flow area asymmetry and additional scrubbing due to the cowl bump over the AGB location, where applicable. The all-electric power extraction, for example, could be achieved by a gearbox (core-gearbox)-mounted PM-generator/starter, which would have the same advantages as the IEG/S and also avoids frontal area increase. This might be a preliminary first step in the direction of reliable electrical secondary power systems.

B. APPLICABILITY OF SELECTED IEG/S AND ENGINE TO FUTURE PROPULSION SYSTEMS

The applicability and feasibility of the Level III IEG/S with SmCo, Permanent Magnet technology have been proven by analytical methods provided in this report. The IEG/S applicability to other engines can be predicted, but a detailed study must be performed for each individual application.

Low bypass or pure jet engines (single spool) for high performance aircraft with near-sonic or supersonic operational speed have the highest potential payoff for an IEG/S due to frontal area drag reduction (especially in wing-pod- or fuselage-pod-mounted configurations). This application would be most effectively integrated into the design of a new low bypass engine.

The IEG/S concept would also have a good application in a simplified form (no safety disconnect) and Power Level I configuration for a cruise missile application. Further studies are recommended that would examine the payoff and application potential of the IEG/S for military aircraft. This study would preferably be a cooperative effort between an airframe manufacturer and a propulsion engine manufacturer. Three distinct types of military aircraft (fighter, transport, and RPV) should be studied to conclude assessments of the worth of the IEG/S concept at the aircraft system level.

APPENDIX - DRAWINGS

TABLE OF CONTENTS

SECTION 1.0 IEC/(S) CONCEPTS
LOW BYPASS TURBOFAN (F404)

SECTION 2.0 IEG/(S) CONCEPTS HIGH BYPASS TURBOFAN (F103/CF6)

POWER LEVEL	DRAWING NUMBER	TITLE
		LAYOUTS - IEG/(S) INTERFACES
I	4013186-987	75/90 KVA Cylindrical
I	4013186-989	75/90 KVA 3 Disk
II	4013186-988	120 KVA Cylindrical
II	4013186-990	120 KVA 3 Disk
IIIA	4013271-030	300 KVA Cylindrical
IIIA	4013271-031	300 KVA 12 Disk
		FULL ENGINE CROSS SECTIONS WITH IEG/(S)
I	4013271-208	75/90 KVA Cylindrical
II	4013271-209	120 KVA Cylindrical
IIIA	4013271-210	300 KVA Cylindrical
IIIA	4013271-211	300 KVA 12 Disk

SECTION 3.0 IEG (S) CONCEPTS SMALL TURBOFAN (TF34)

POWER LEVEL	DRAWING NUMBER	TITLE
		LAYOUTS - IEG/(S) INTERFACES
I	4013186-977	30/40 KVA Cylindrical
I	4013186-976	30/40 KVA 3 Disk
II	4013186-972	60 KVA Cylindrical
II	4013186-973	60 KVA 4 Disk
IIIA	4013271-026	60/75 KVA Cylindrical
IIIA	4013271-027	60/75 KVA 6 Disk
III	4013271-028	120 KVA Cylindrical
III	4013271-029	120 KVA 9 Disk
		FULL ENGINE CROSS SECTIONS WITH IEG/(S)
I	4013271-200	30/40 KVA Cylindrical
II	4013271-201	60 KVA Cylindrical
III	4013271-202	120 KVA Cylindrical
III	4013271-203	120 KVA 9 Disk

SECTION 4.0 IEG/(S) FINAL SELECTION SMALL TURBOFAN (TF34)

POWER LEVEL	DRAWING NUMBER	TITLE
		LAYOUT - IEG/(S) INTERFACES
III	4013271-257	120 KVA Cylindrical
		FULL ENGINE CROSS SECTION WITH IEG/(S)
III	4013271-199	120 KVA Cylindrical

SECTION 5.0 IEG/S MOCKUP SMALL TURBOFAN (TF34)

DRAWING NUMBER	TITLE
4013271-109	MOCKUP IEG/S 120 KVA
4013271-129	MOCKUP SUPPORT

*U.S.Government Printing Office: 1980 — 657-084/148

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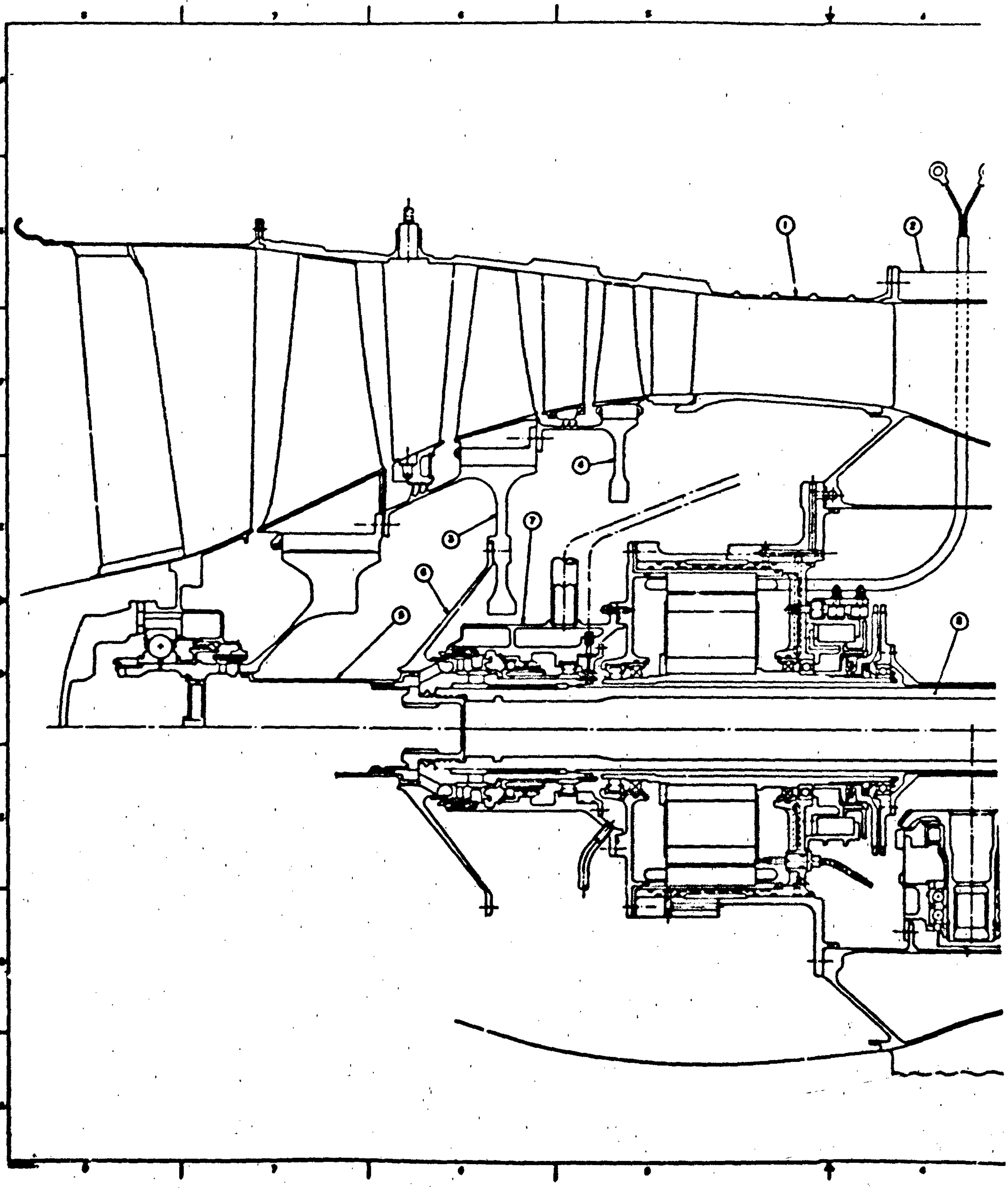

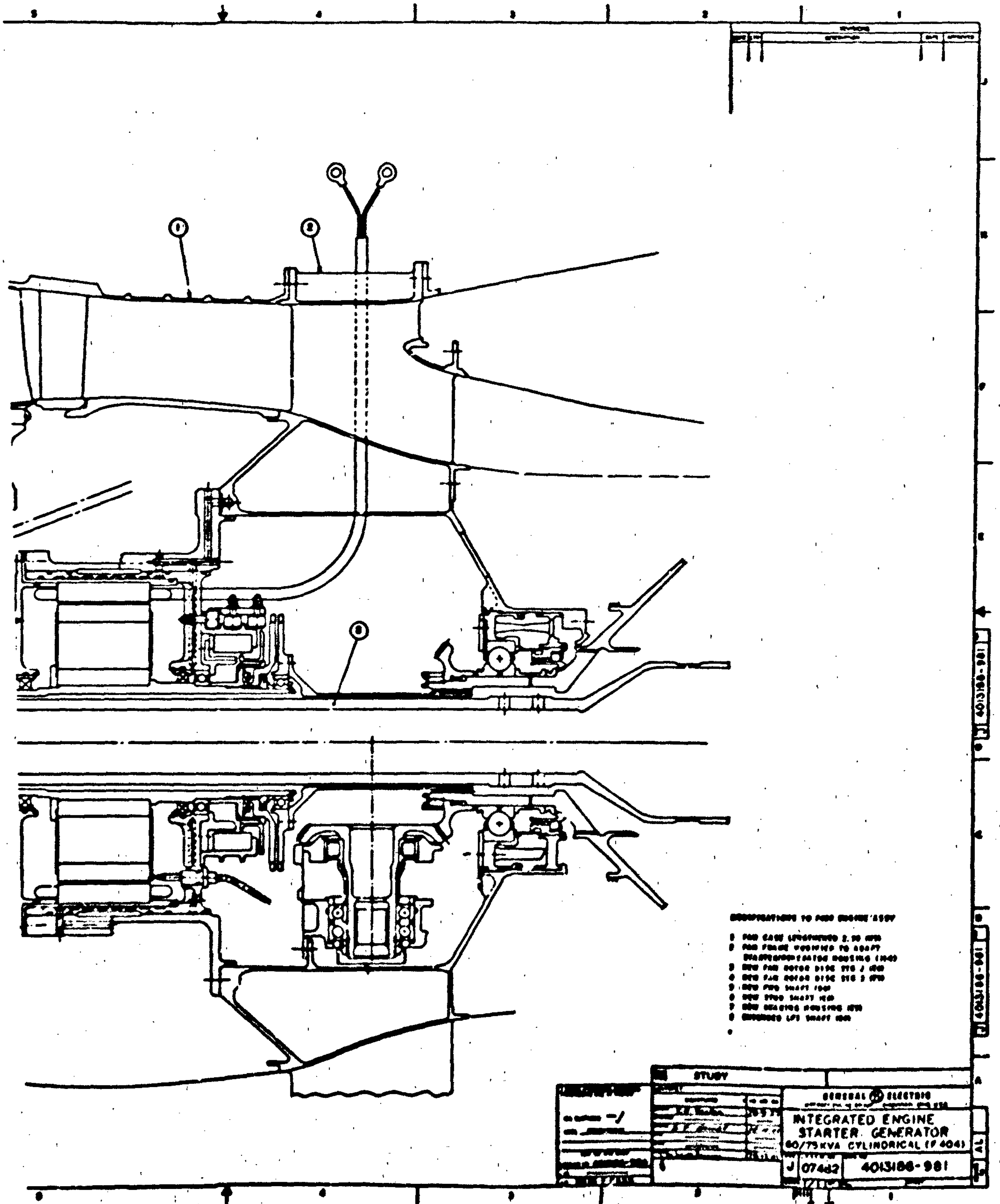
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INTEGRATED ENGINE
STARTER GENERATOR
60/75KVA CYLINDRICAL (F 404)
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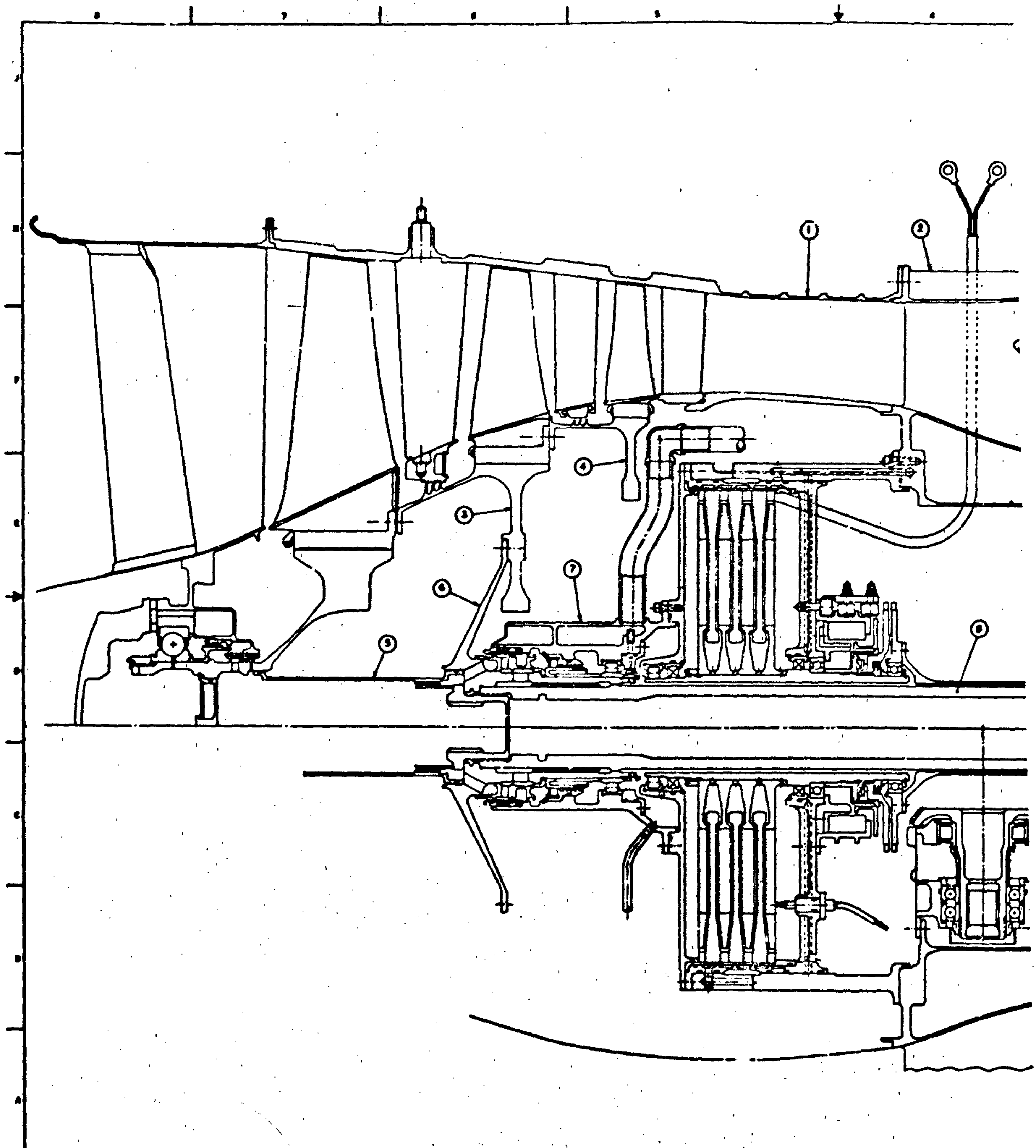
4013186-981



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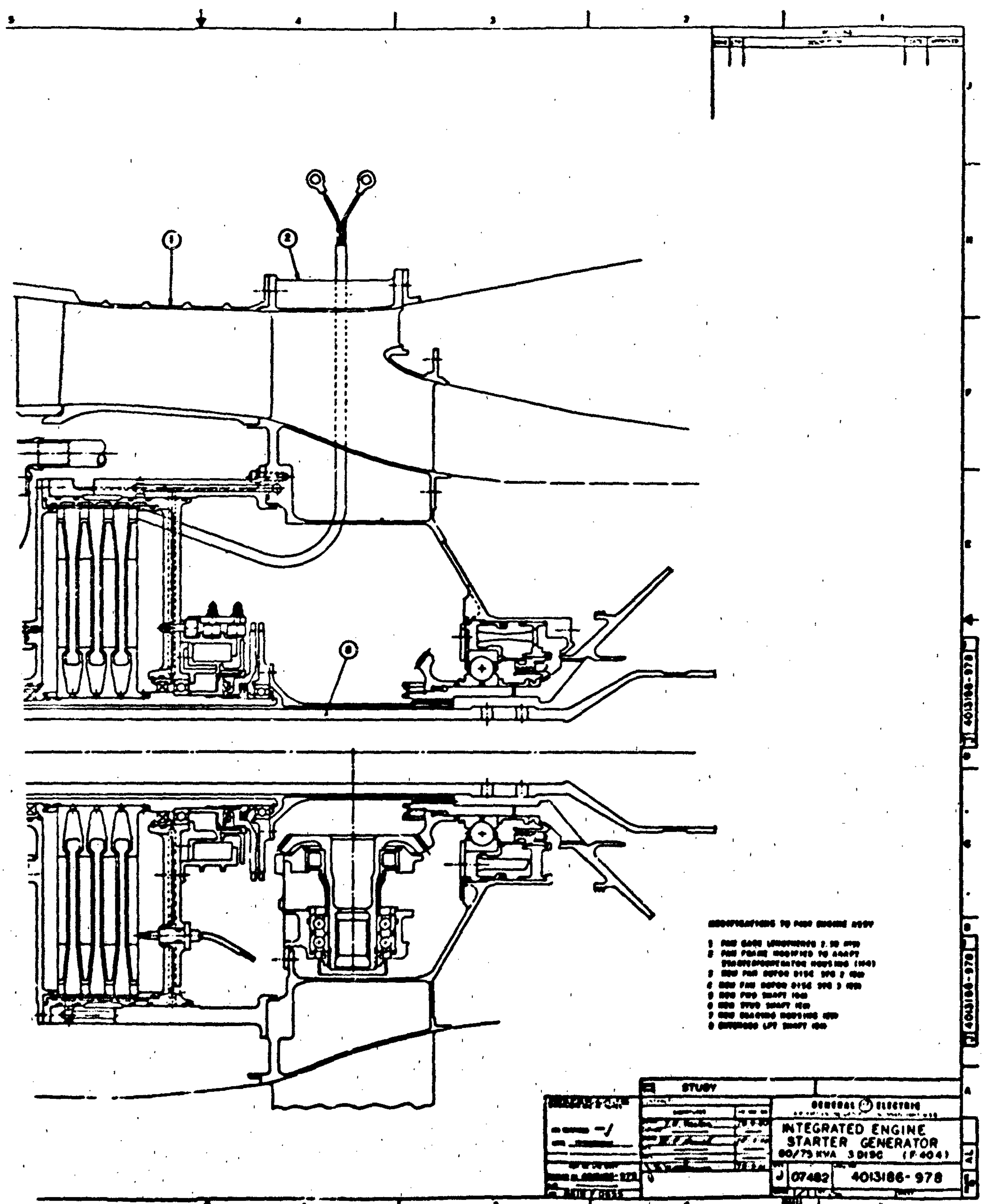

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INTEGRATED ENGINE
STARTER GENERATOR
60/75 KVA 3 DISC (F404)
J 07482 4013186-978
MODIFICATIONS TO F404 ENGINE ASSY
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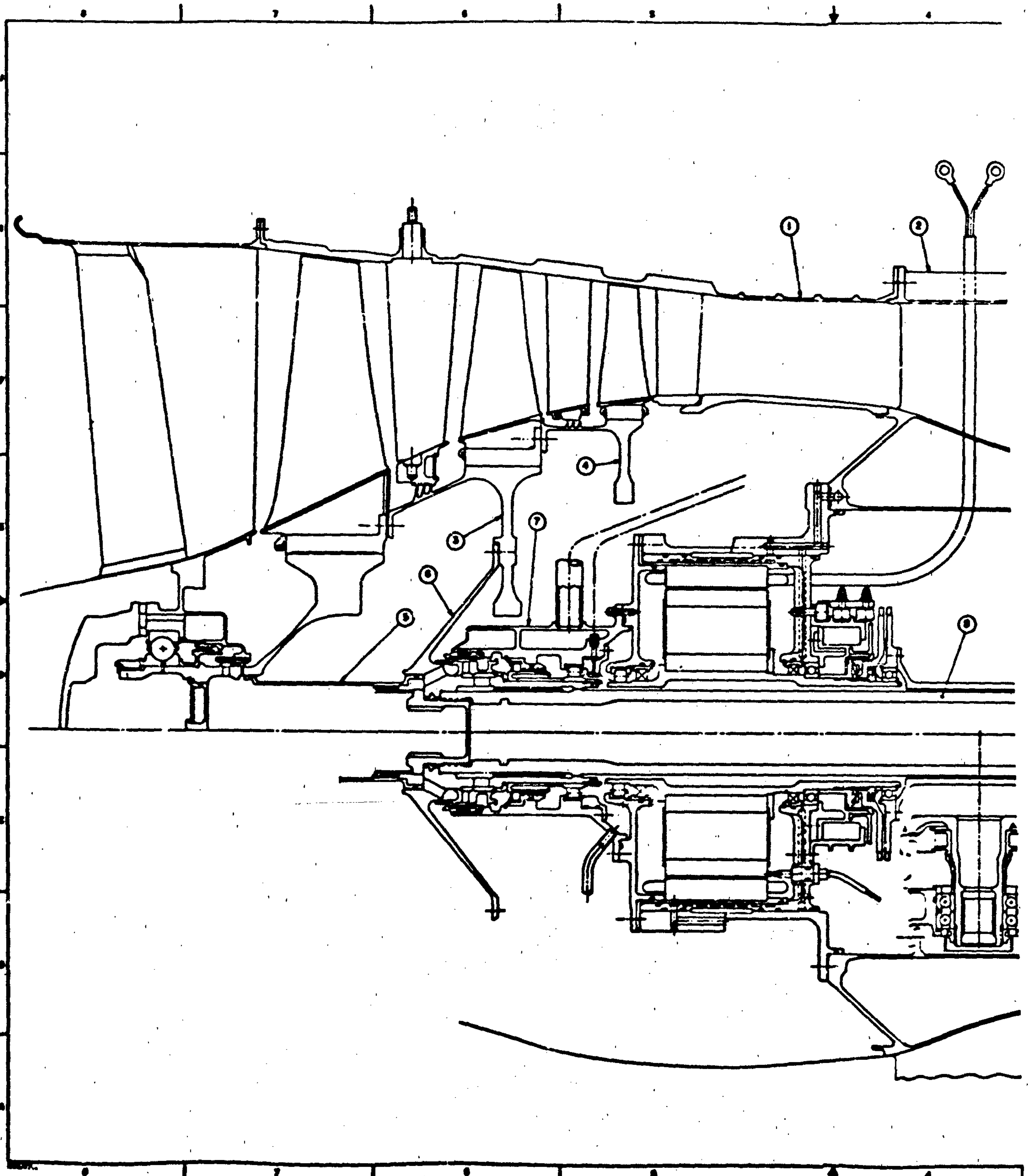

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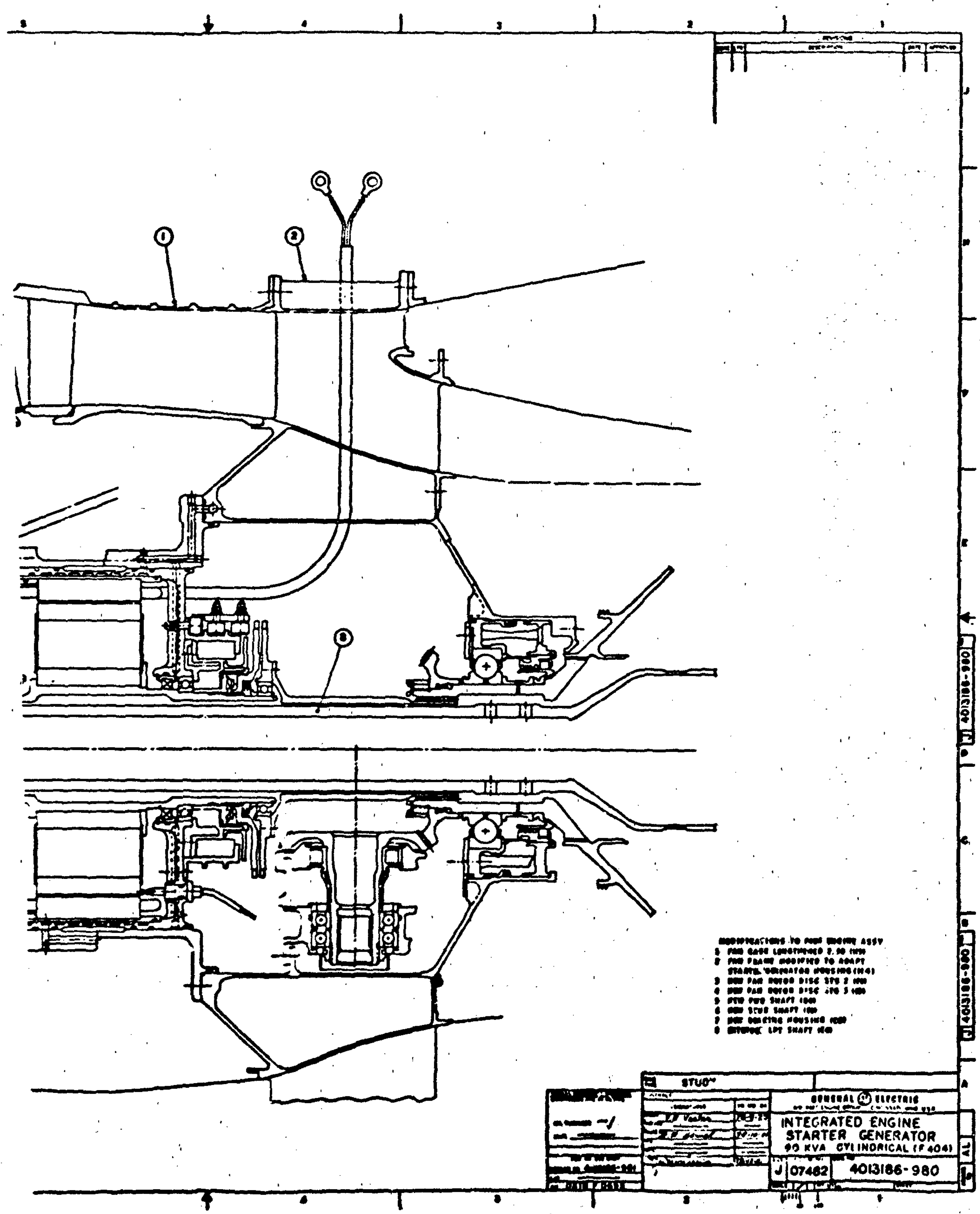
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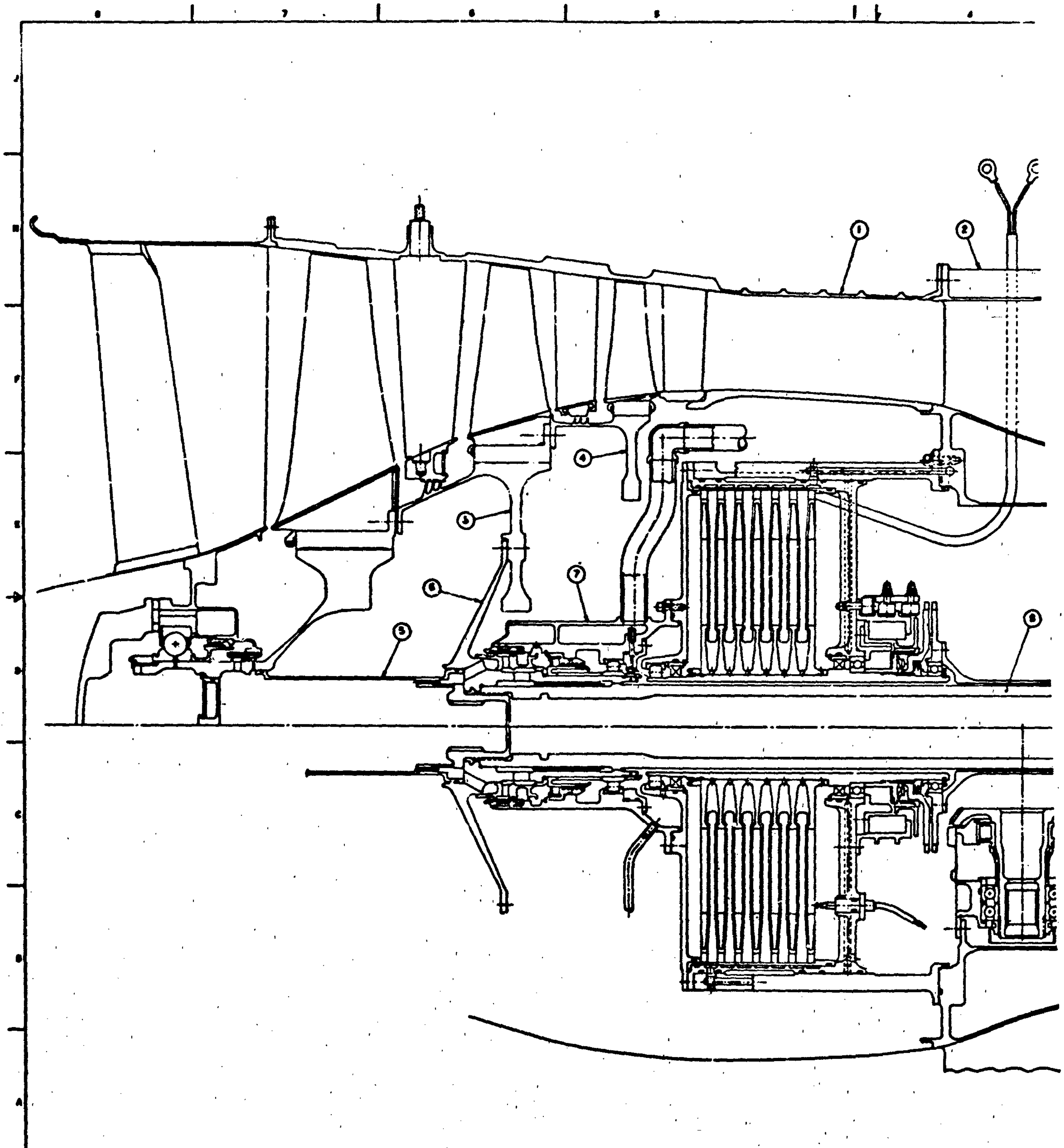

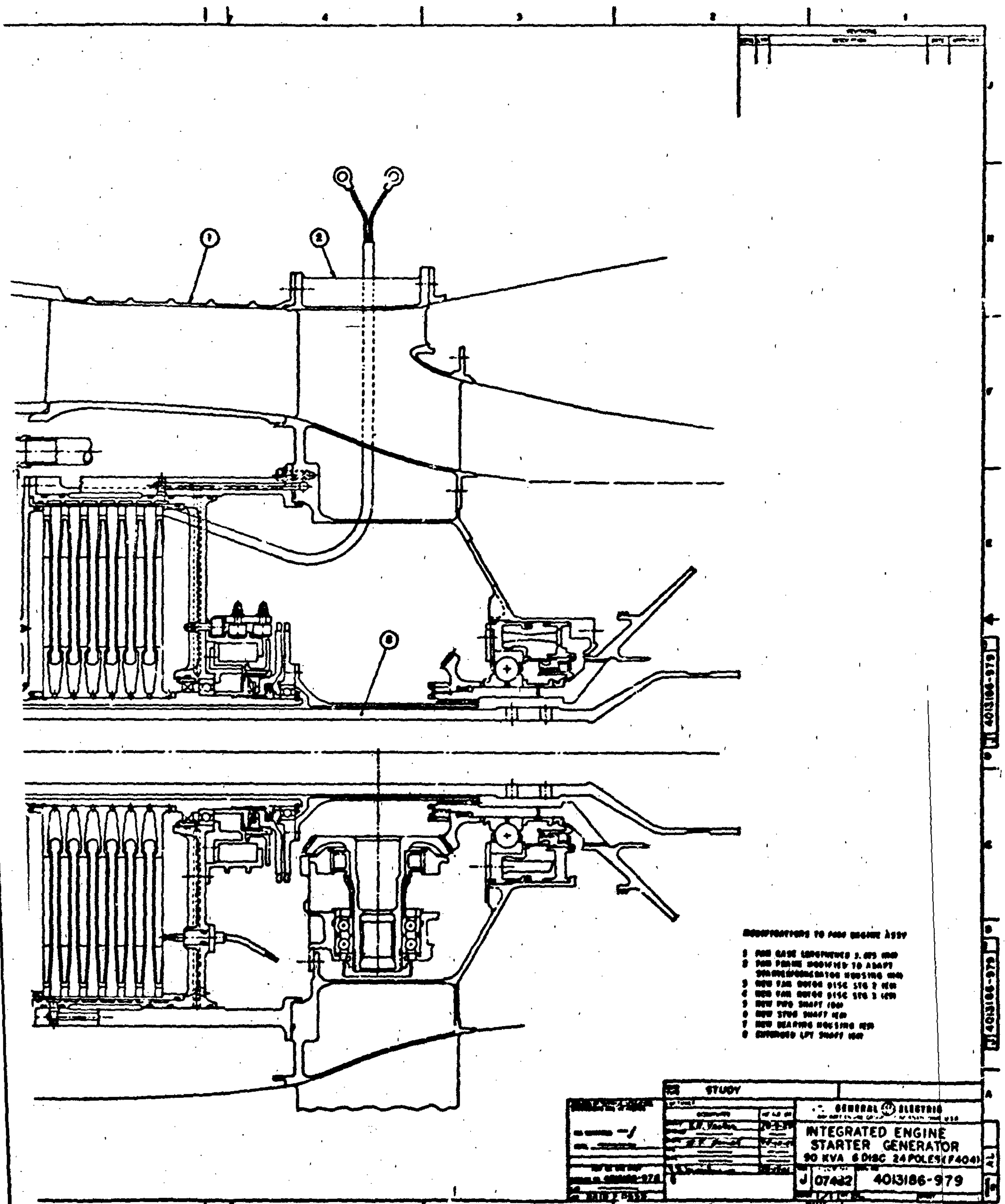

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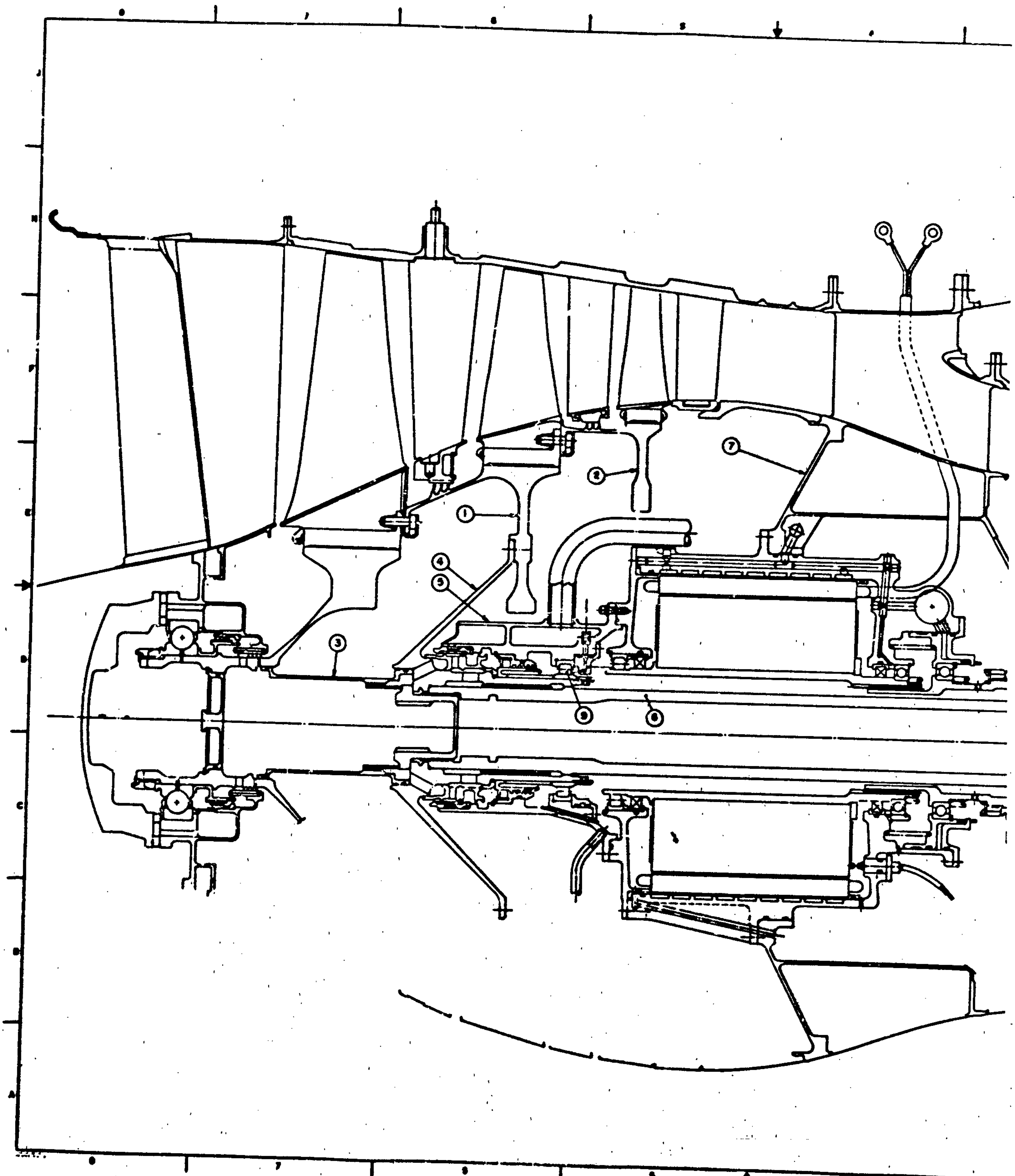
4013186-979



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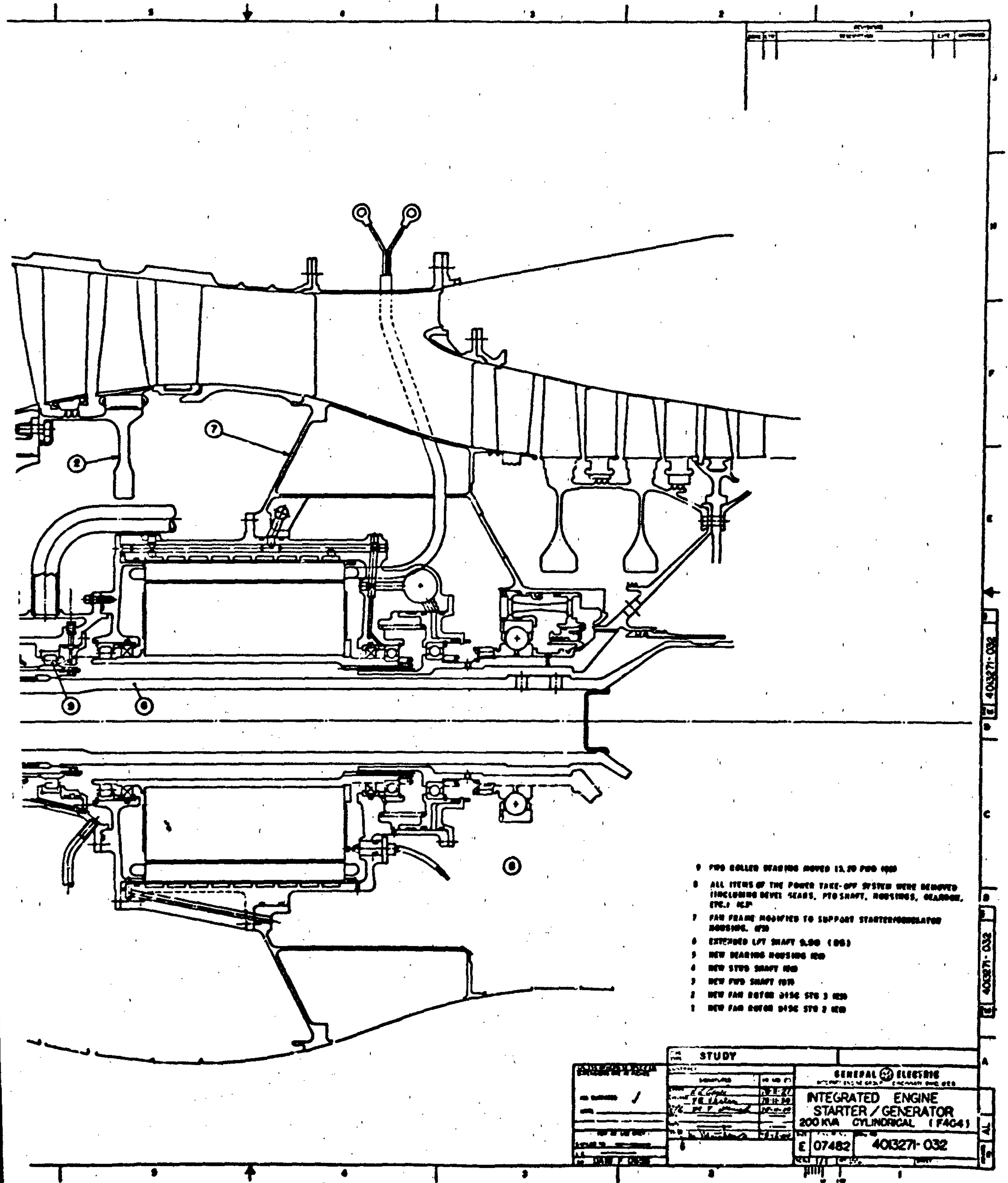

9 FWD ROLLER BEARING MOVED 12.20 FWD
8 ALL ITEMS OF THE POWER TAKE-OFF SYSTEM WERE REMOVED (INCLUDING BEVEL GEARS, PTO SHAFT, HOUSINGS, GEARBOX, ETC.)
7 FAN FRAME MODIFIED TO SUPPORT STARTER/GENERATOR HOUSING
6 EXTENDED LPT SHAFT 9.90
5 NEW BEARING HOUSING
4 NEW STUB SHAFT
3 NEW FWD SHAFT
2 NEW FAN ROTOR DISC STG 3
1 NEW FAN ROTOR DISC STG 2
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STARTER / GENERATOR
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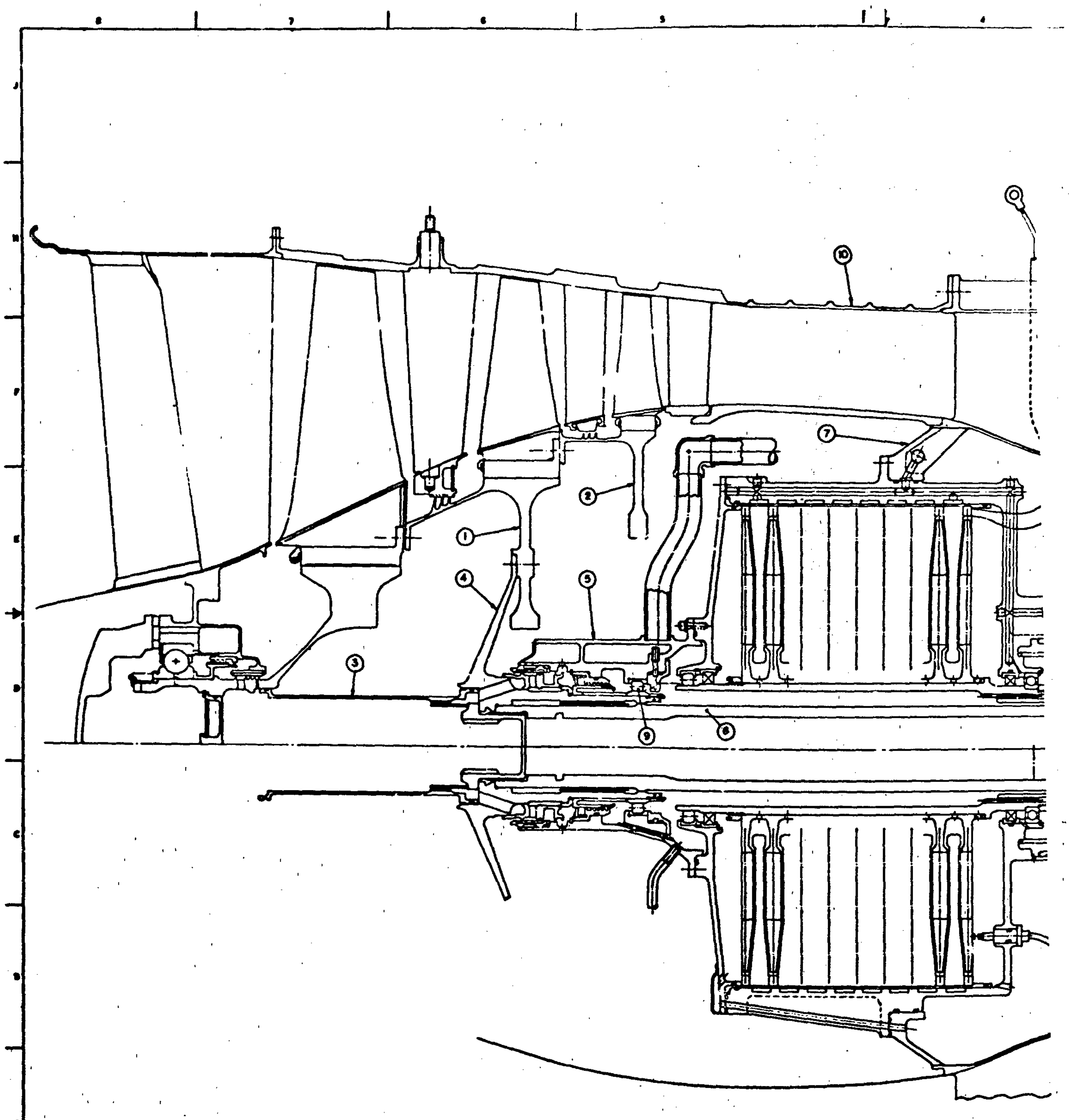

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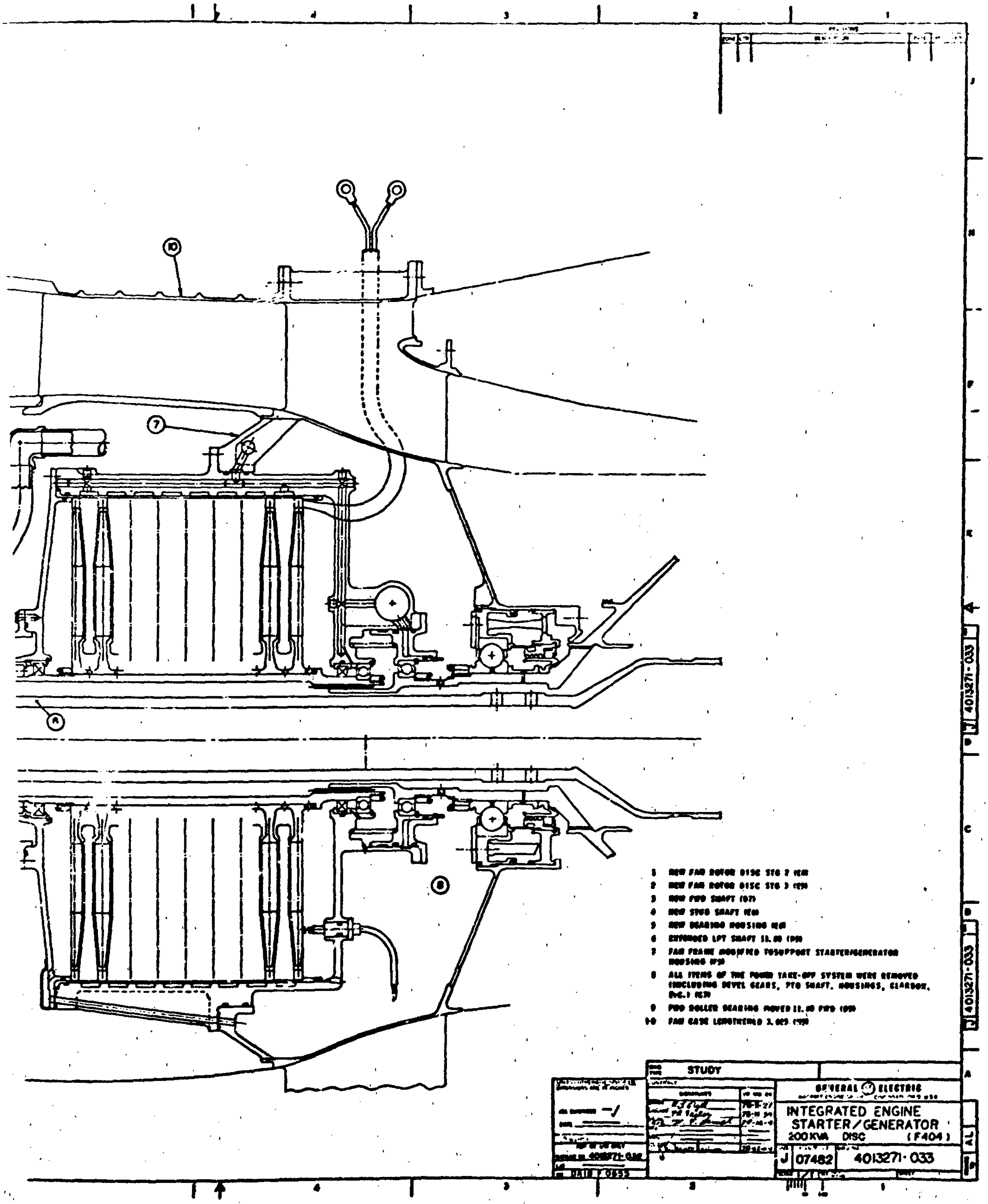
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2 NEW FAN ROTOR DISC STG 3
3 NEW FWD SHAFT
4 NEW STUB SHAFT
5 NEW BEARING HOUSING
6 EXTENDED LPT SHAFT 11.80
7 FAN FRAME MODIFIED TO SUPPORT STARTER/GENERATOR HOUSING
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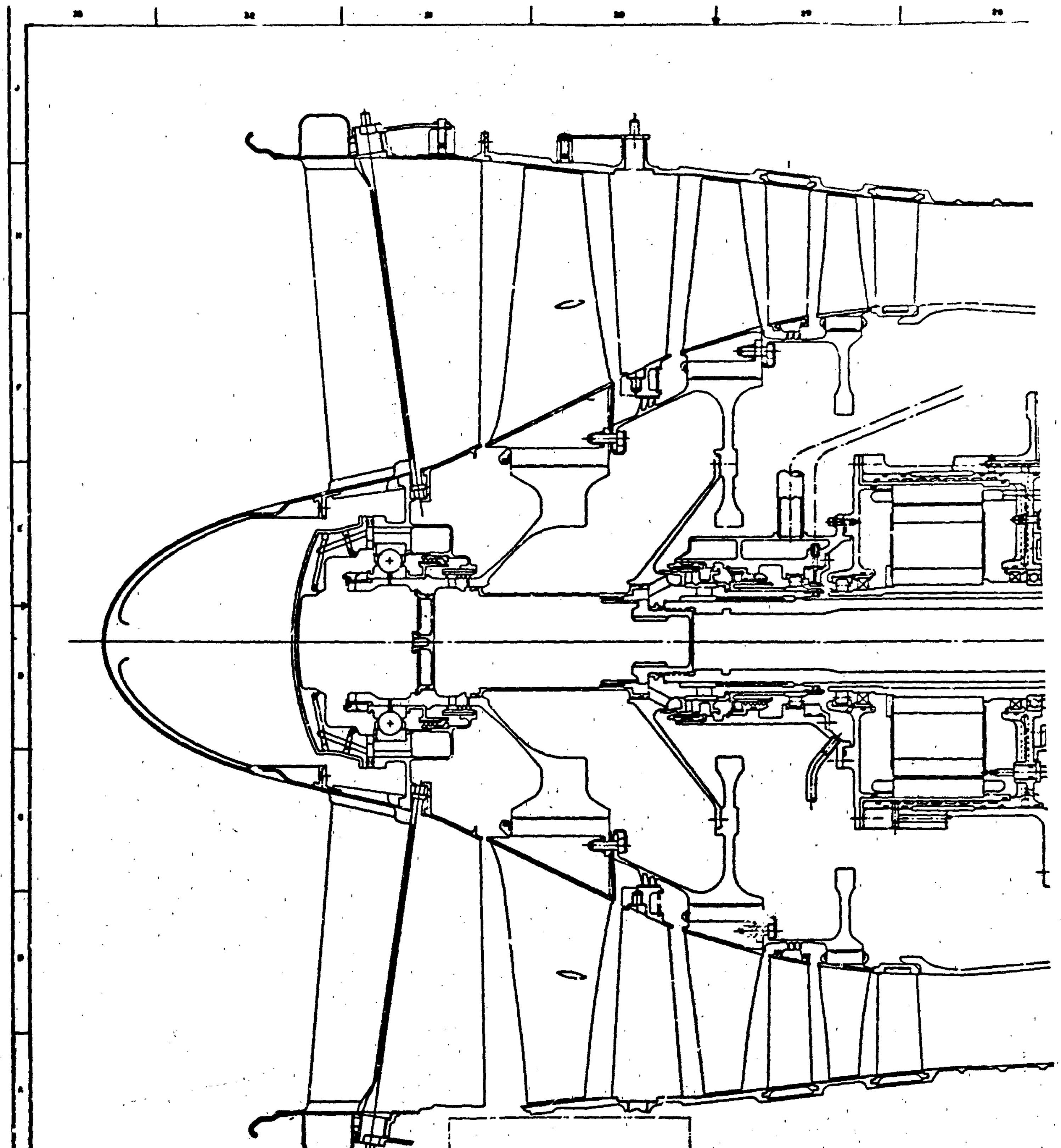

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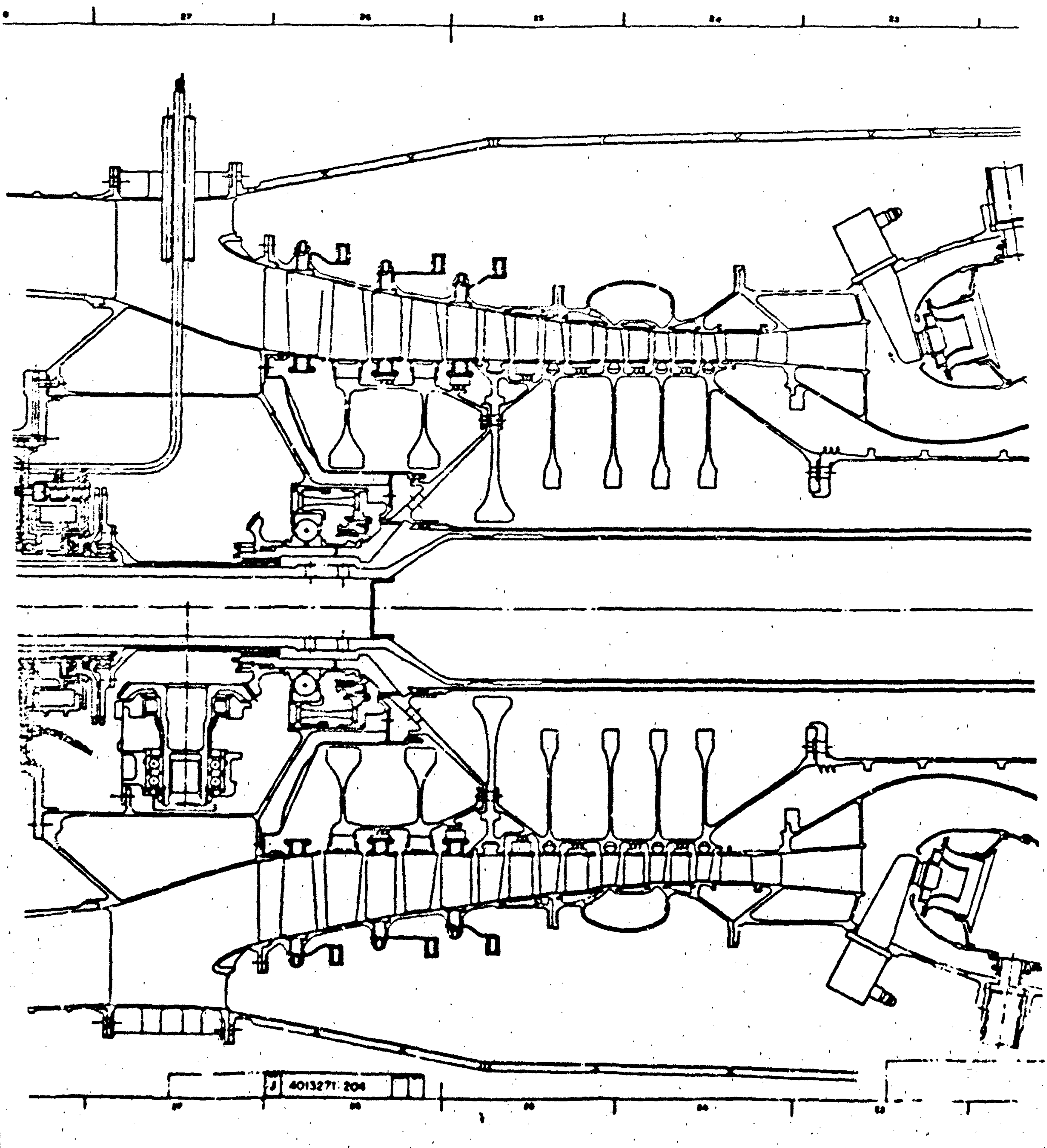

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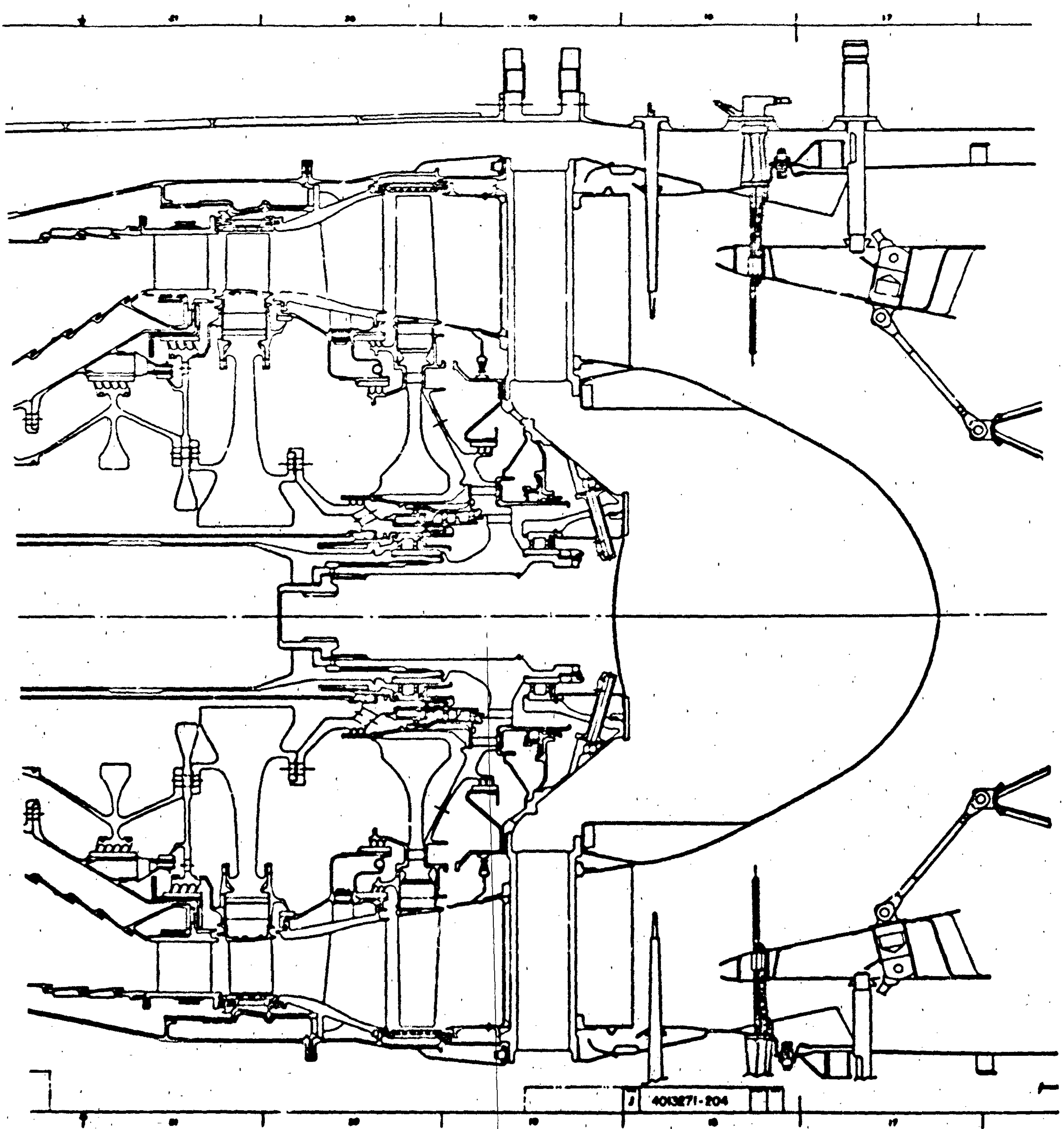

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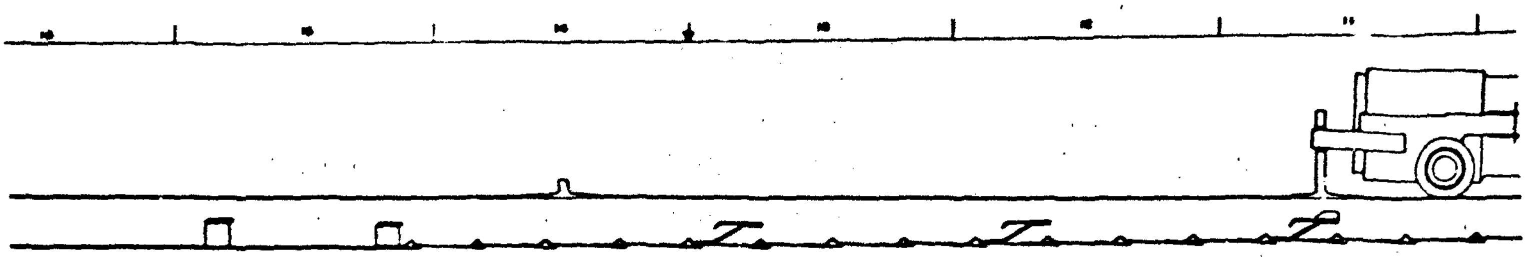

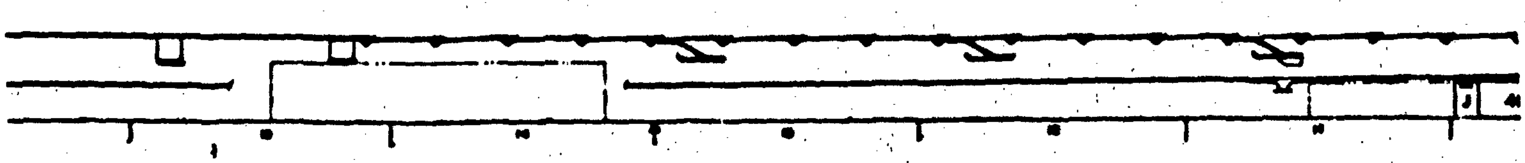

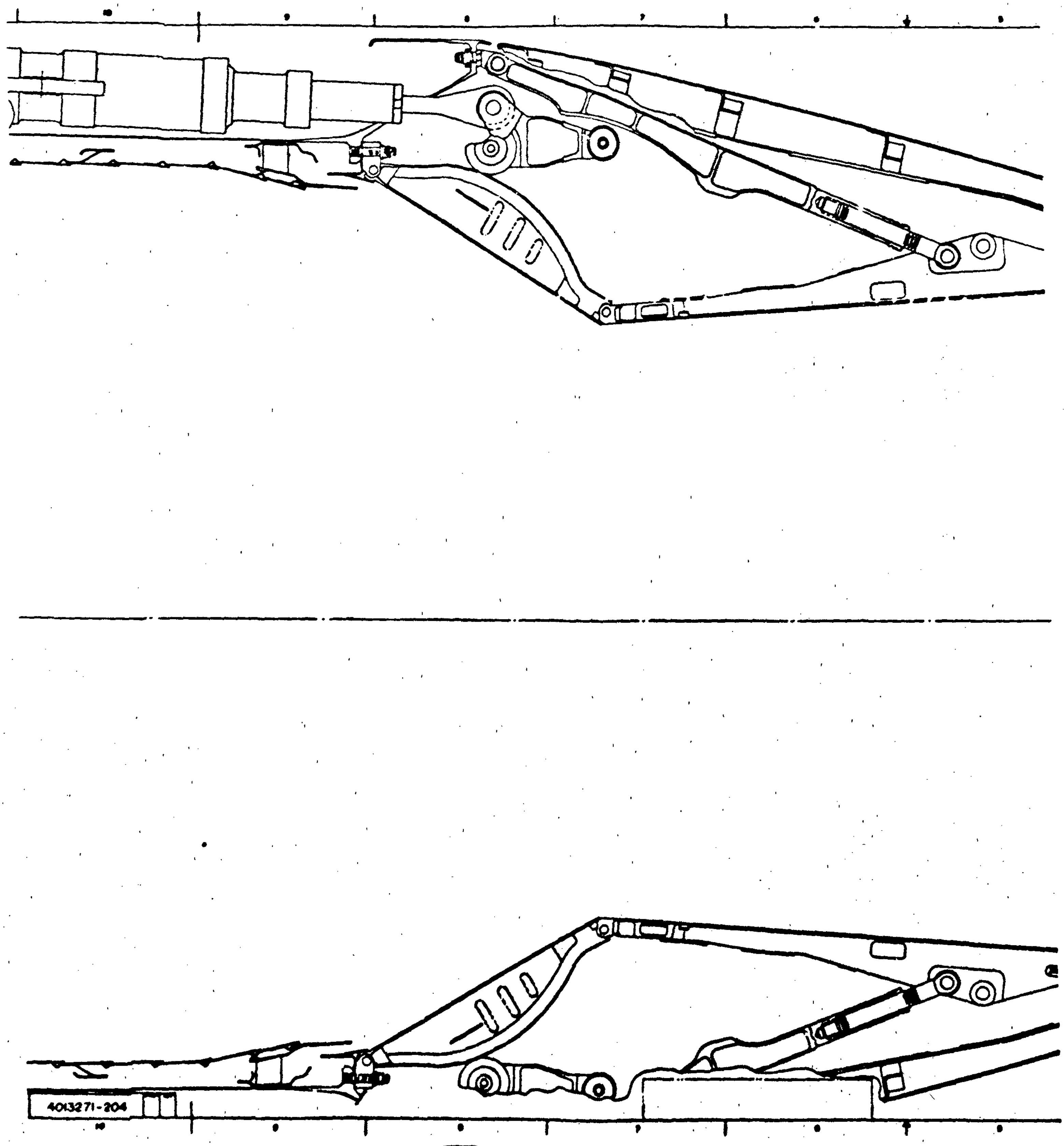

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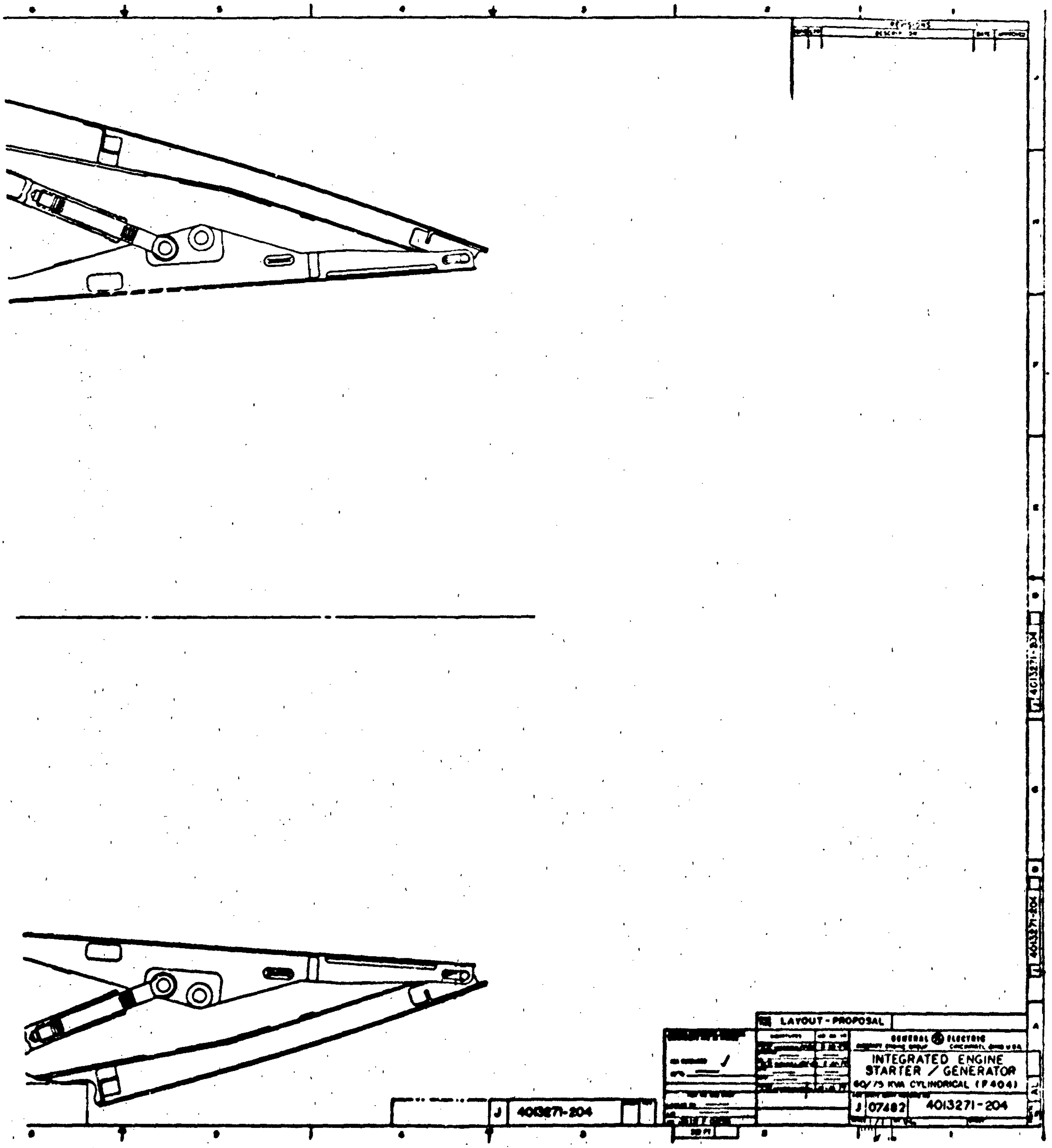

LAYOUT - PROPOSAL
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STARTER / GENERATOR
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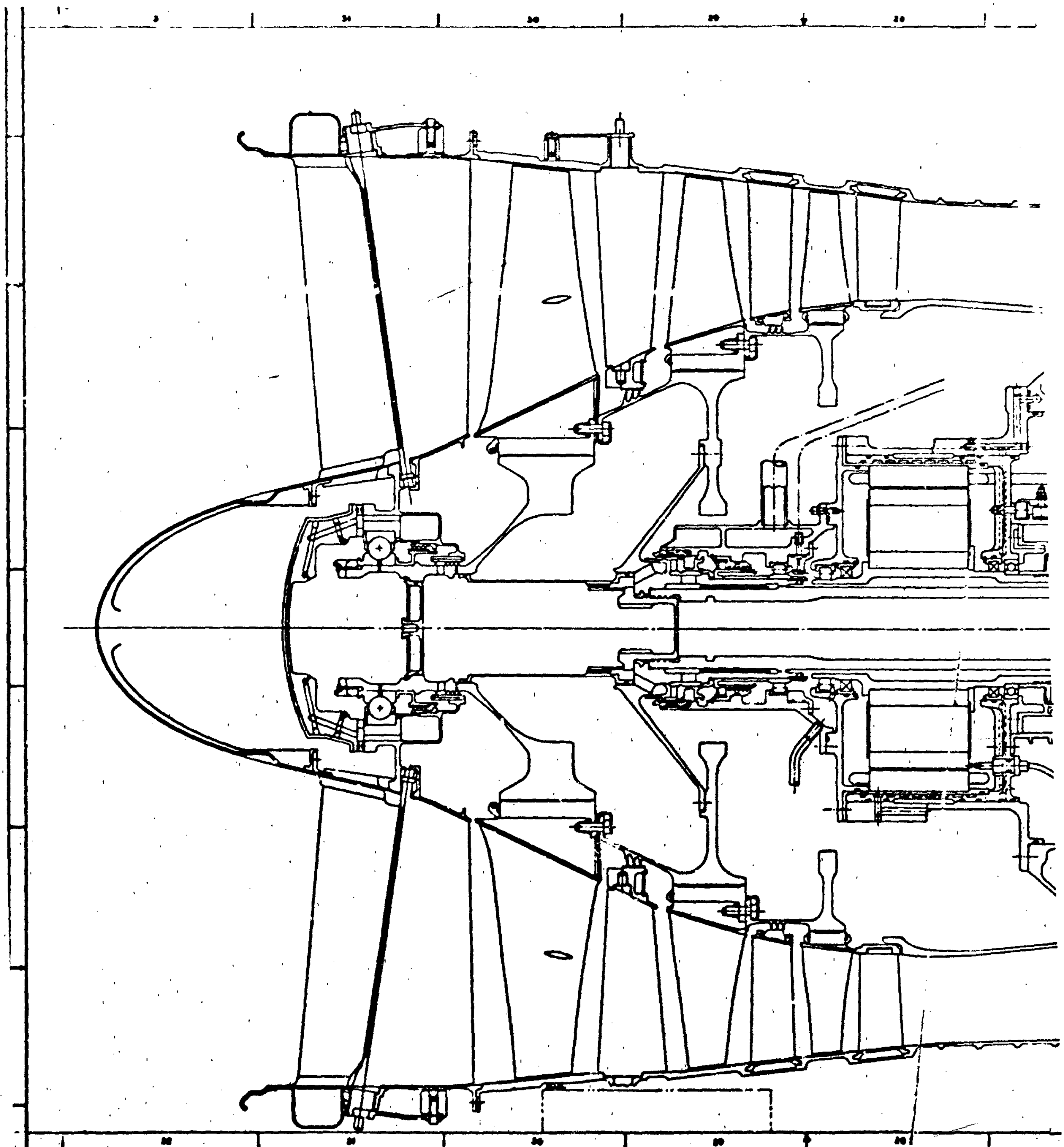
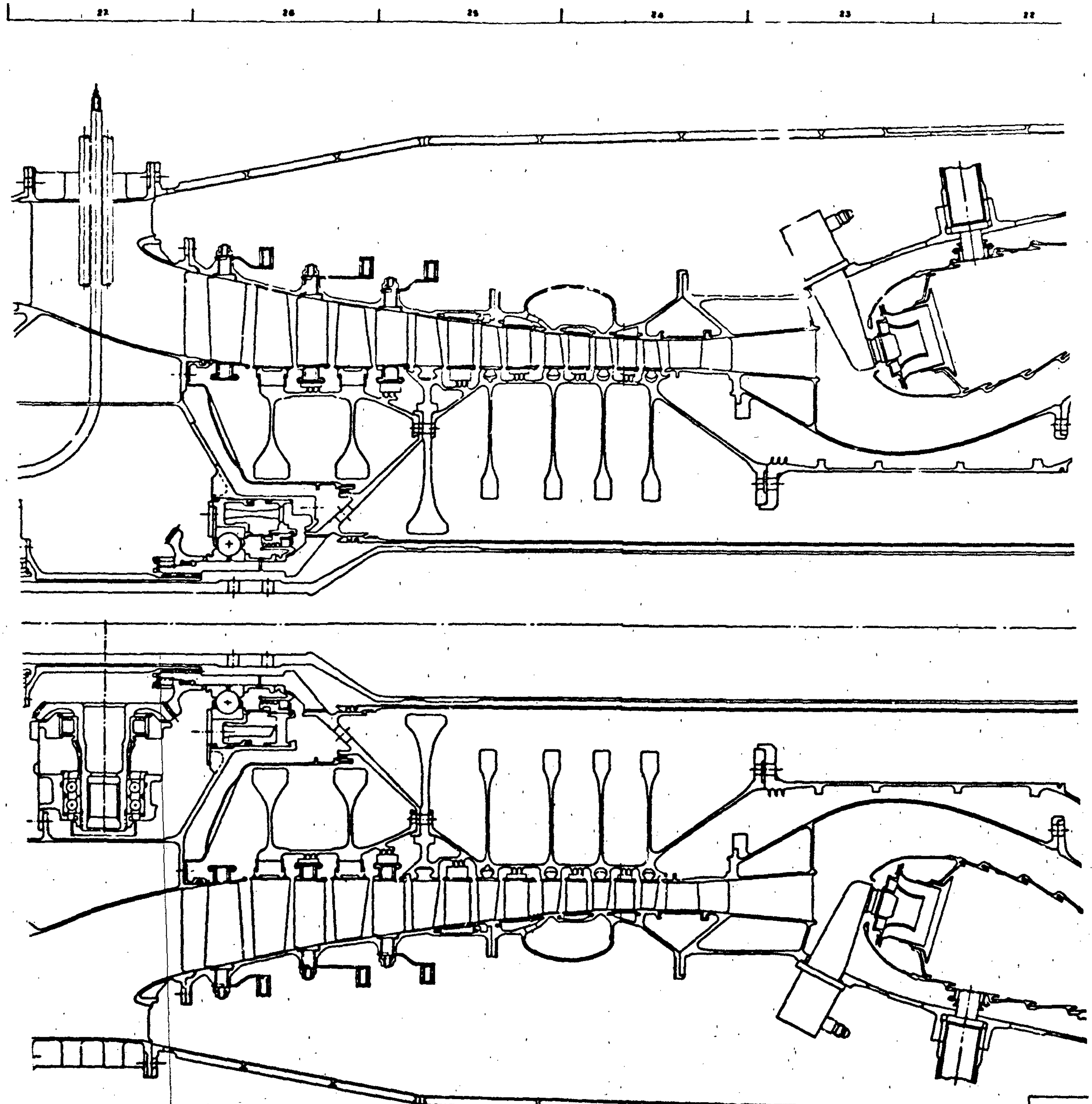
J 4013271-204

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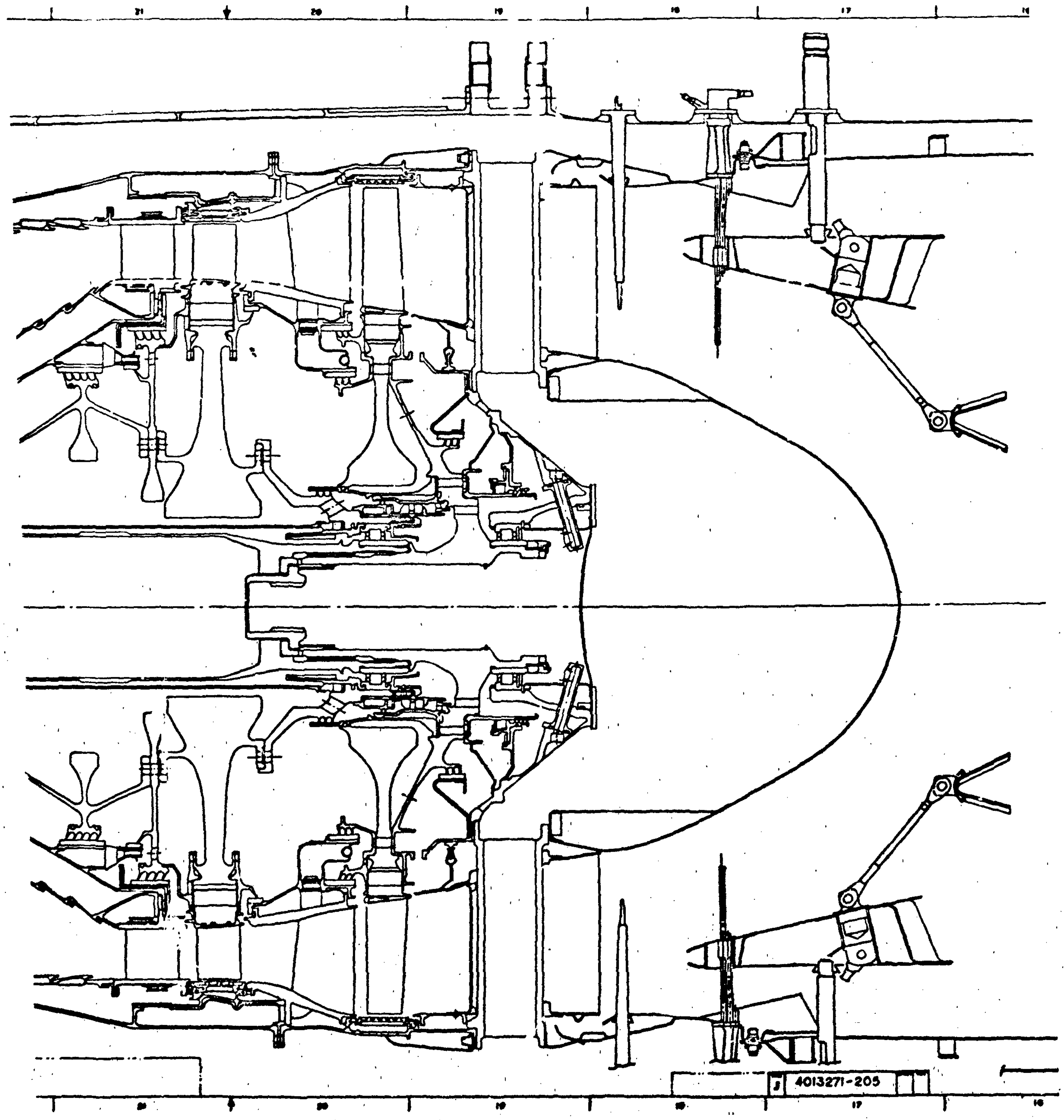

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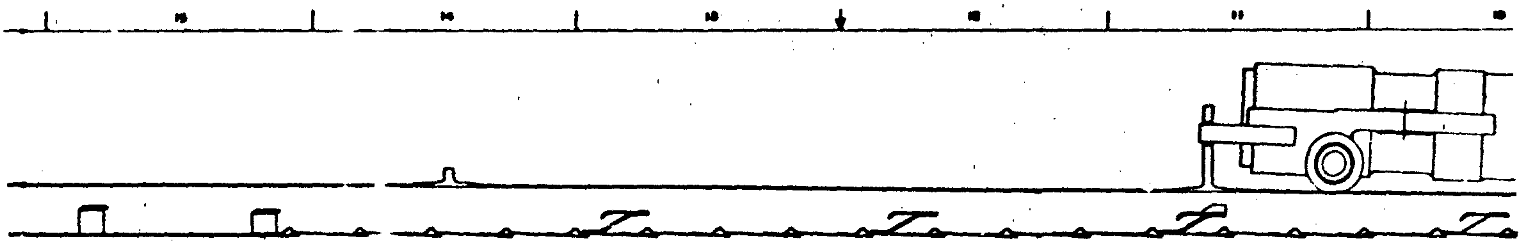

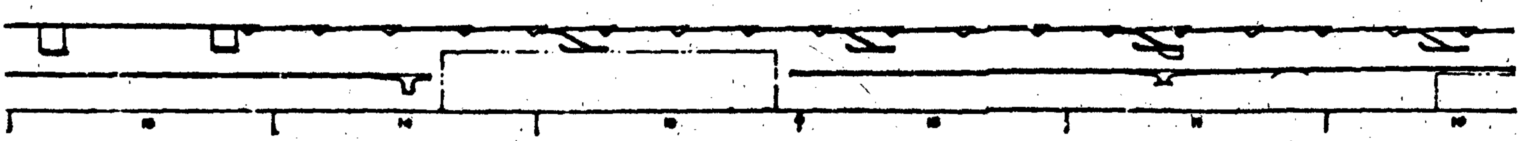

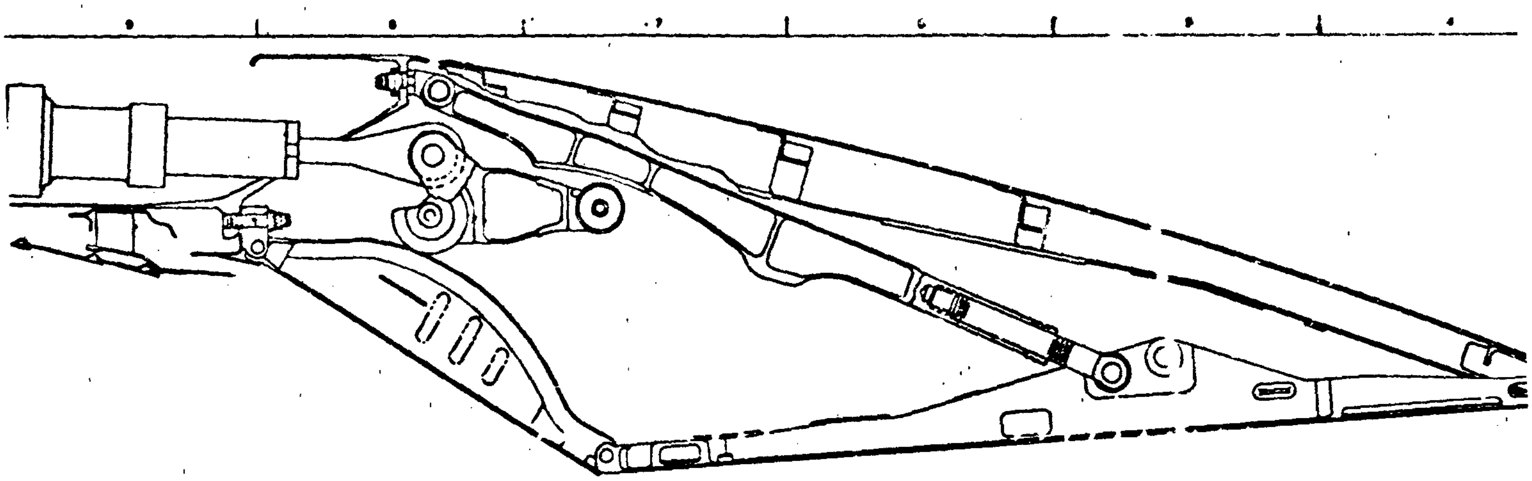

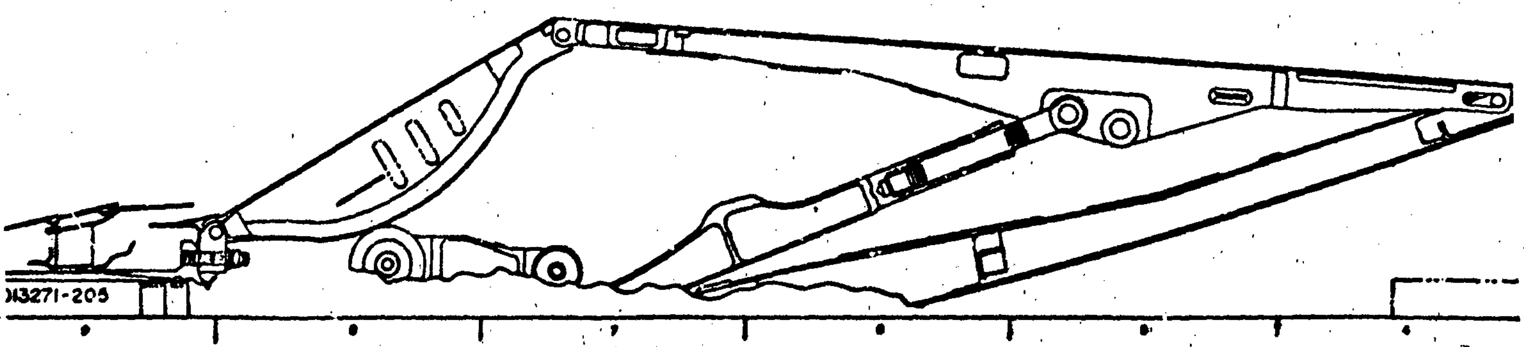

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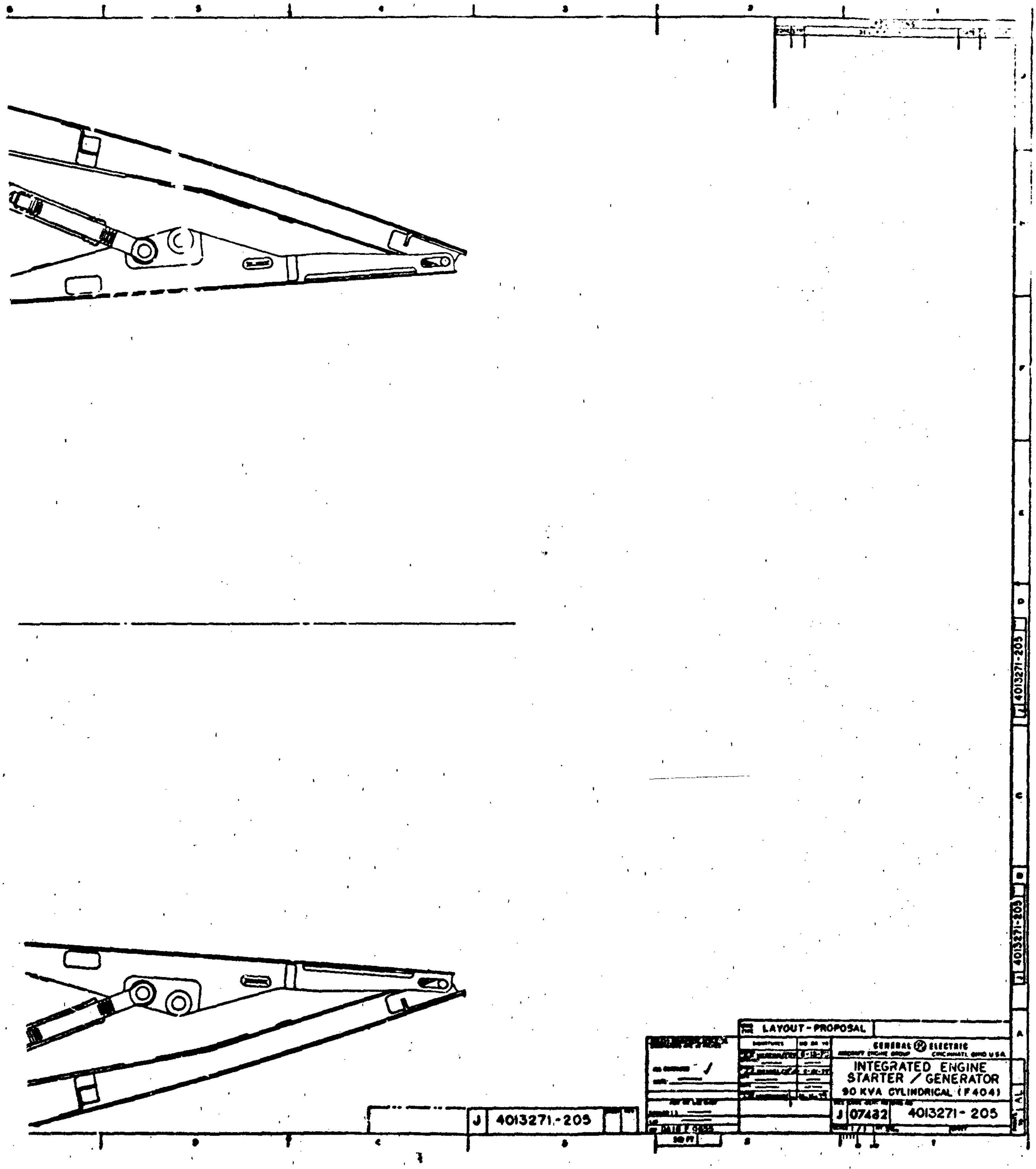

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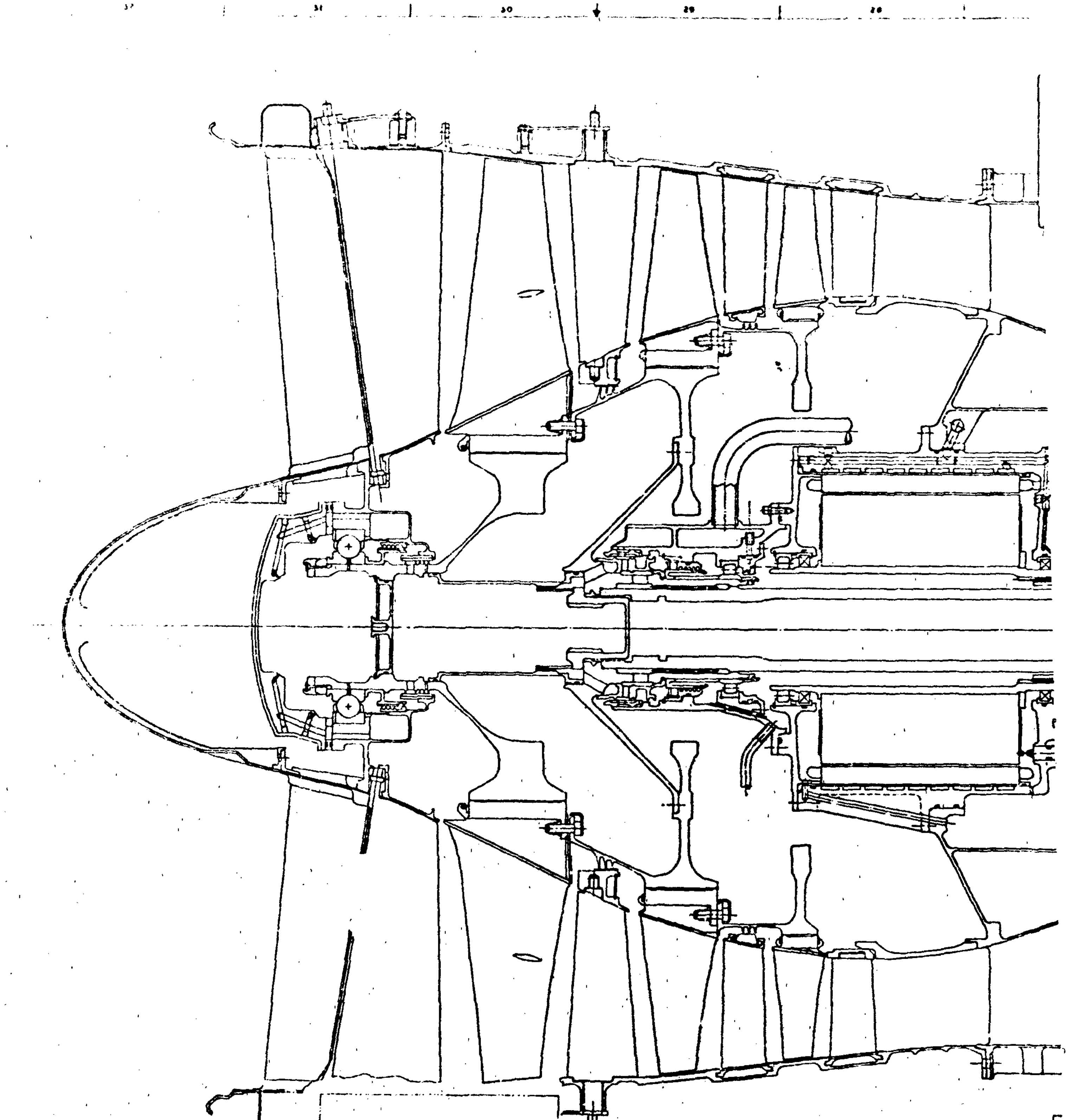

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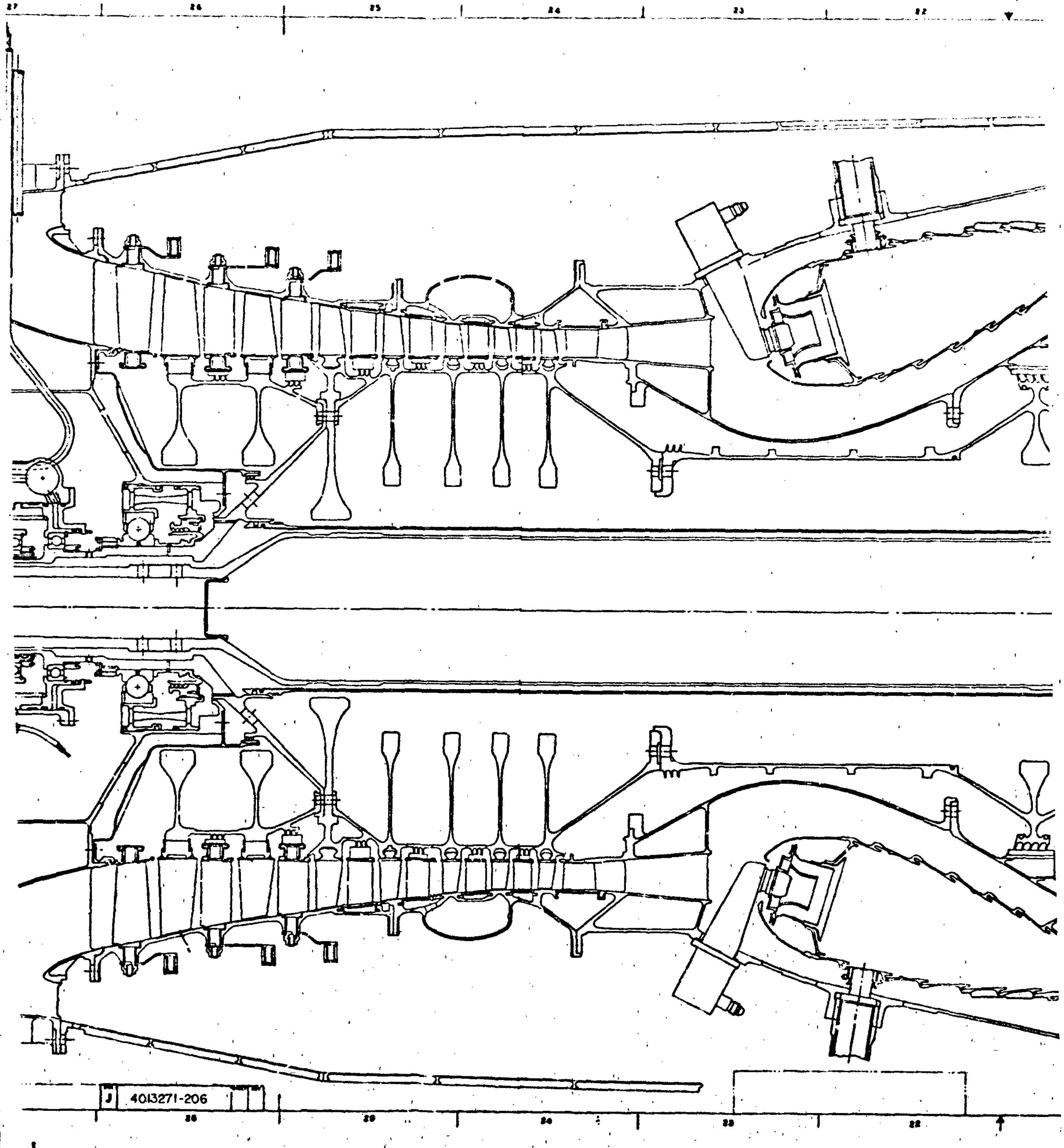
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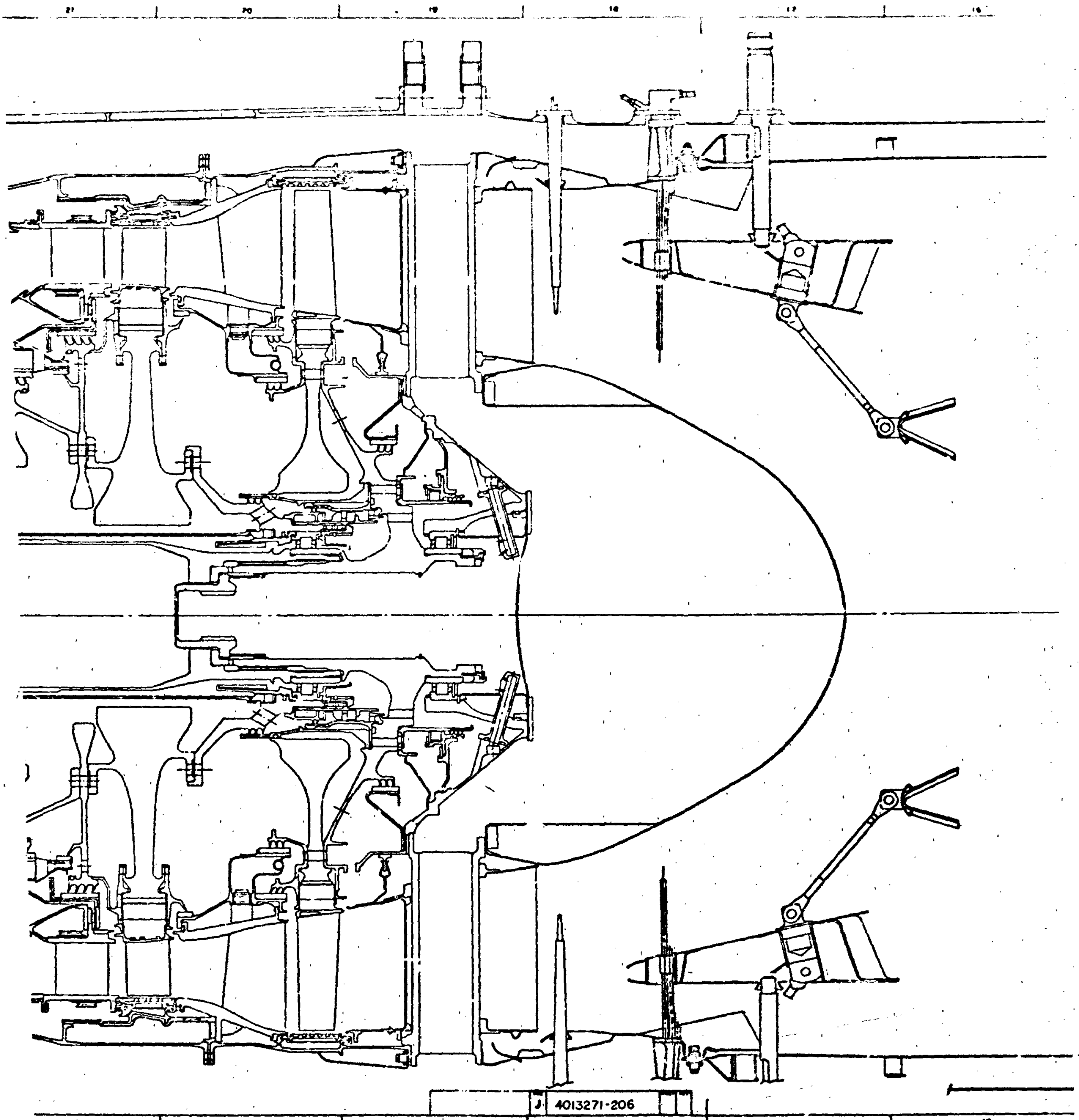

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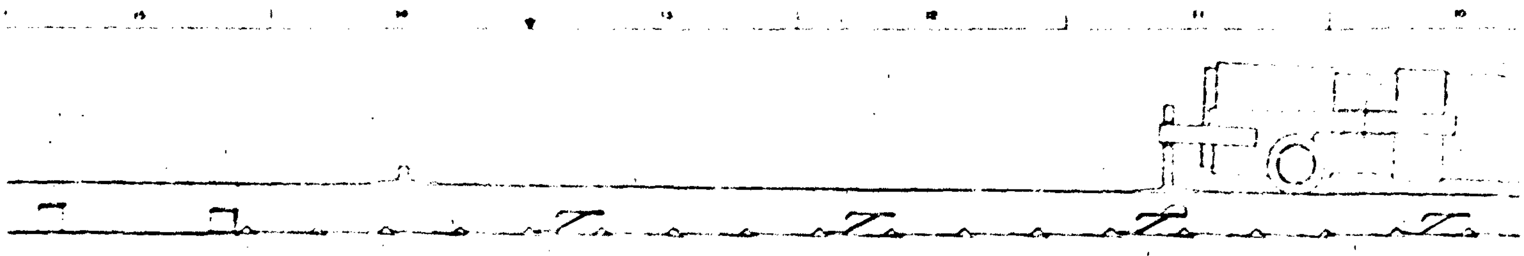

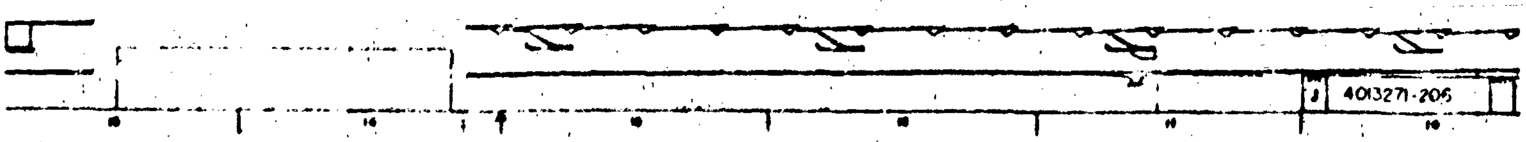
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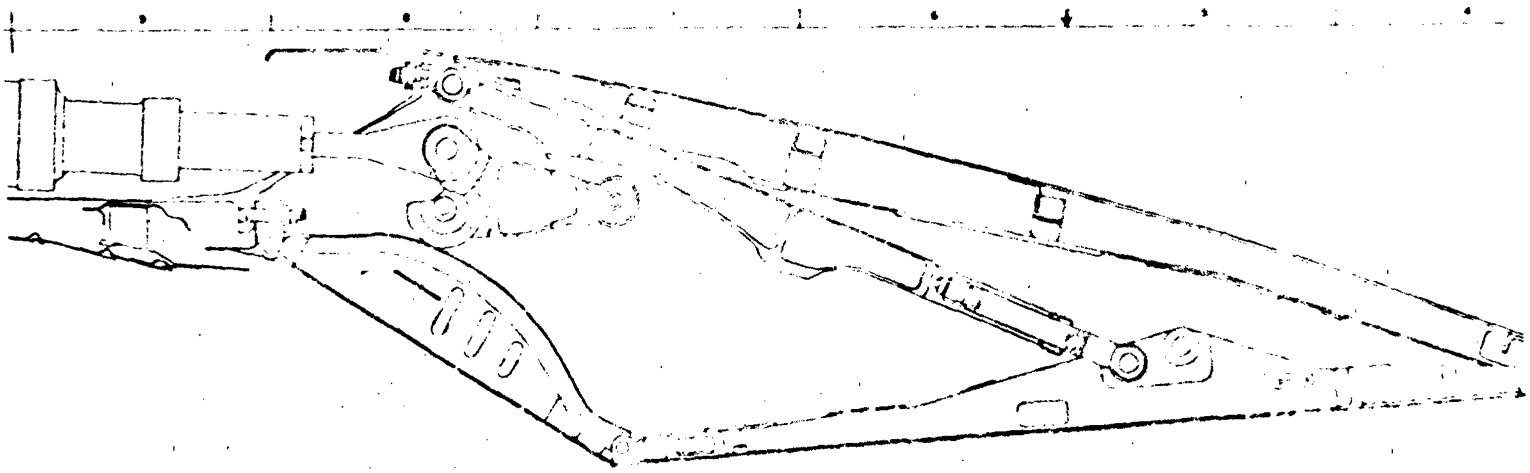

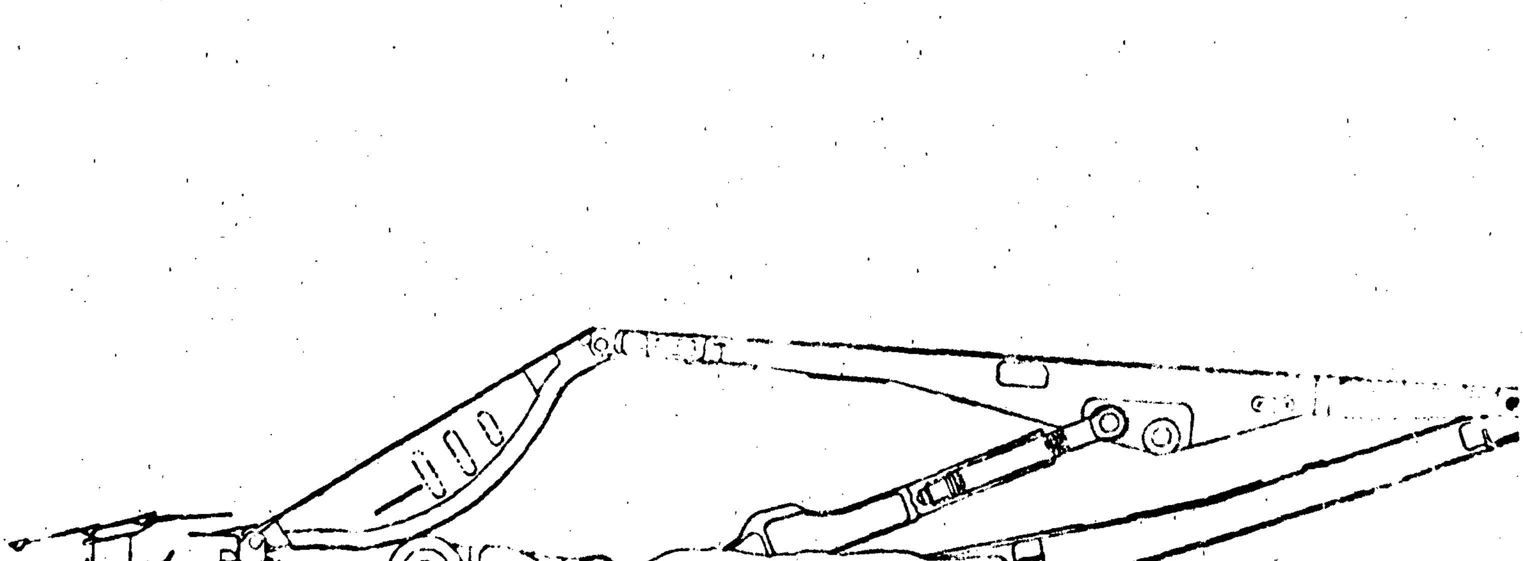

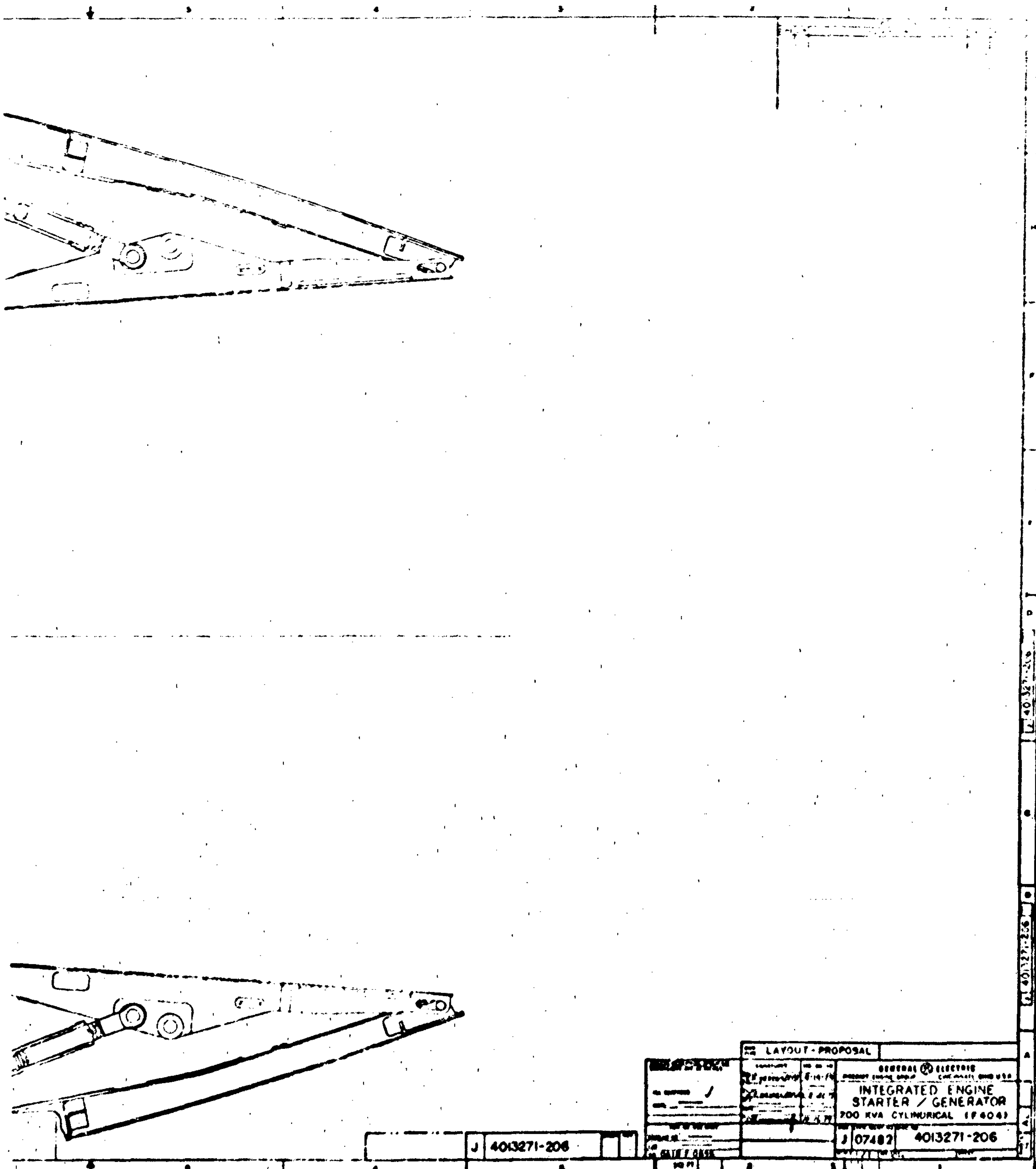

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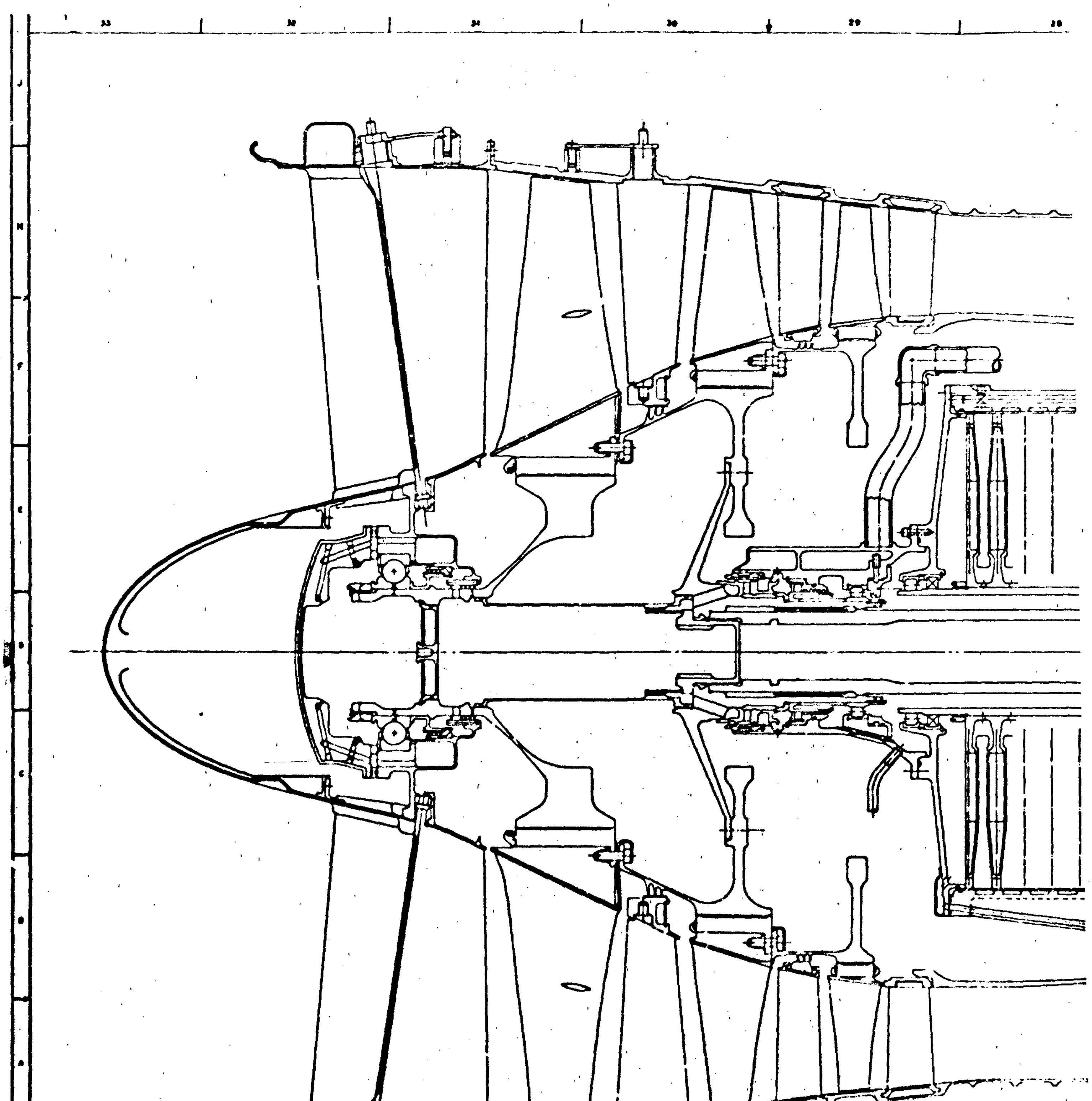

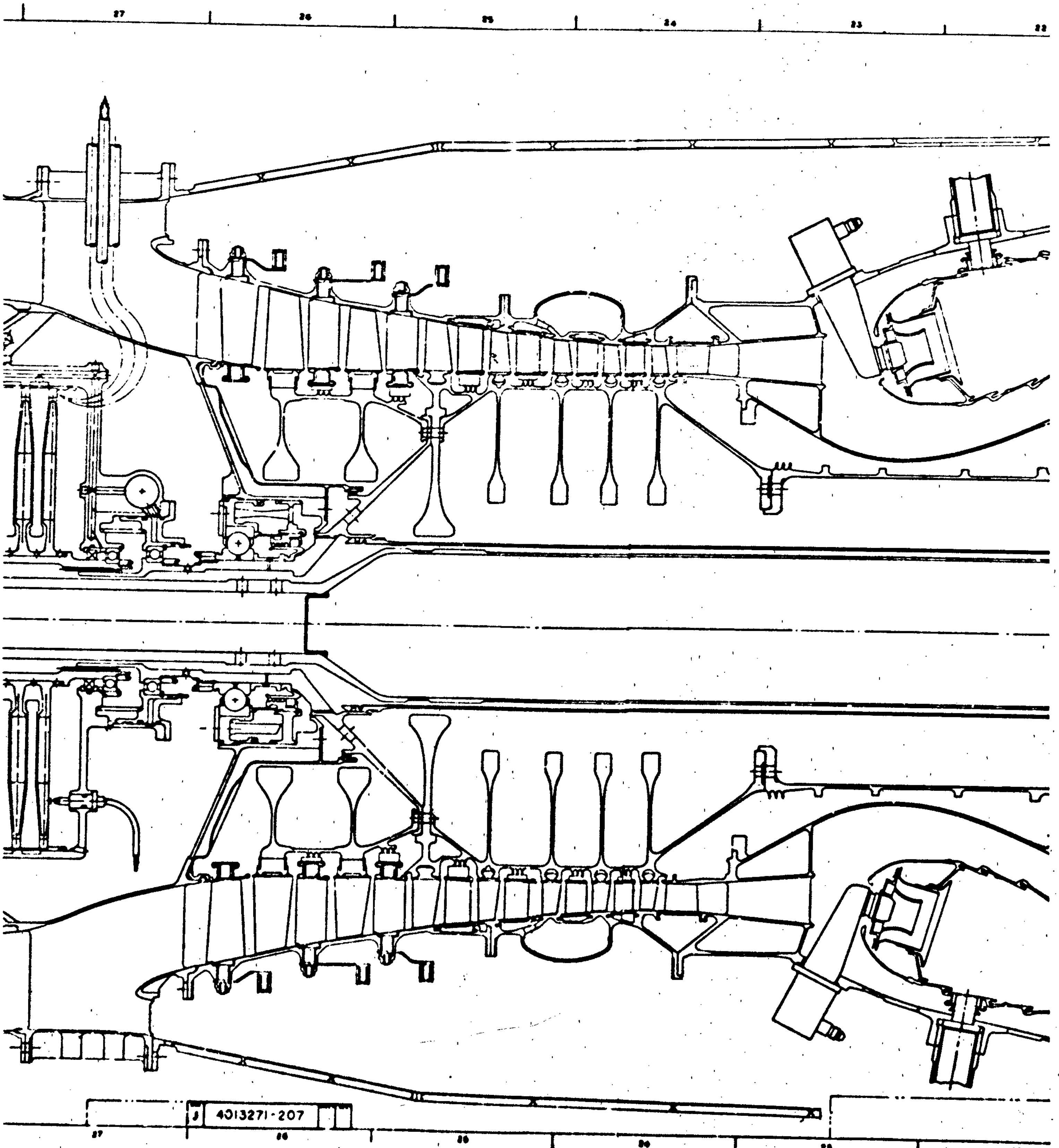

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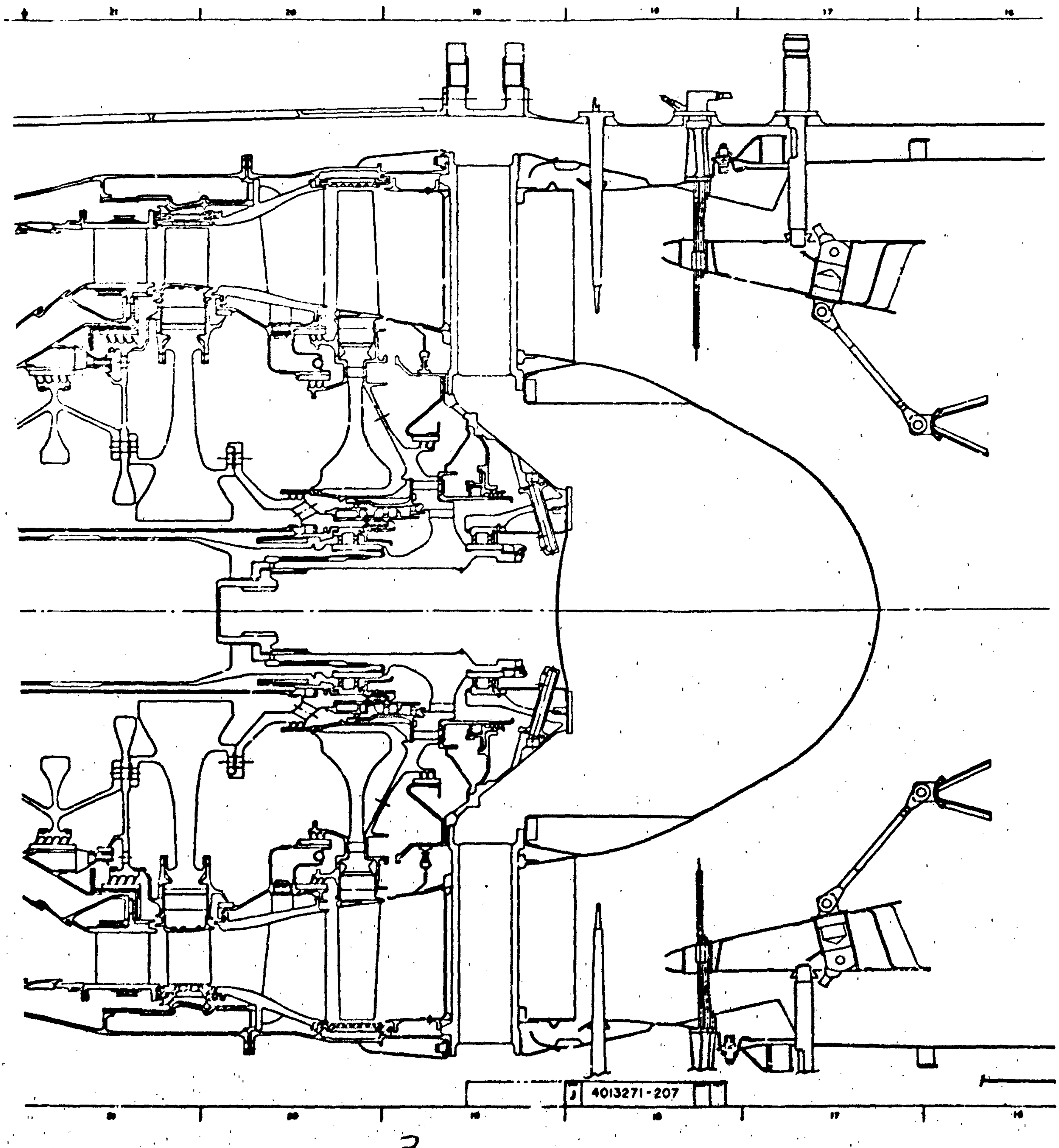
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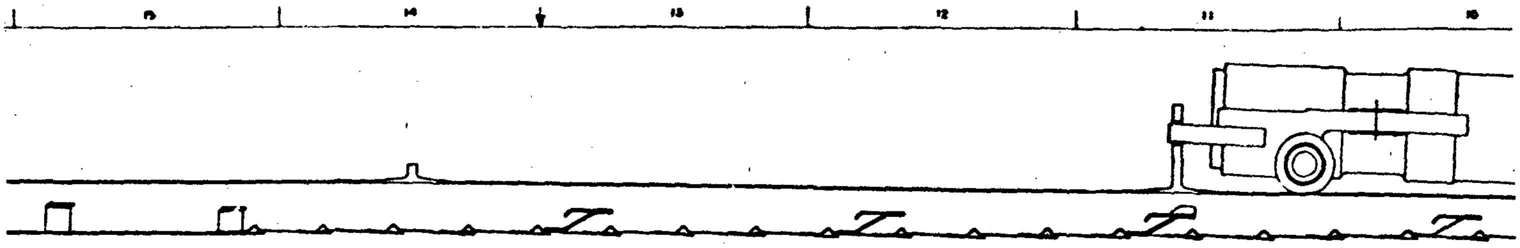

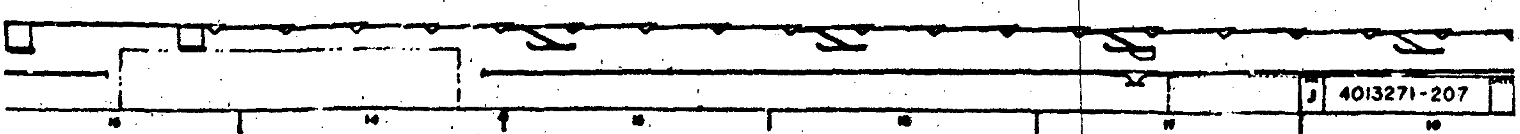
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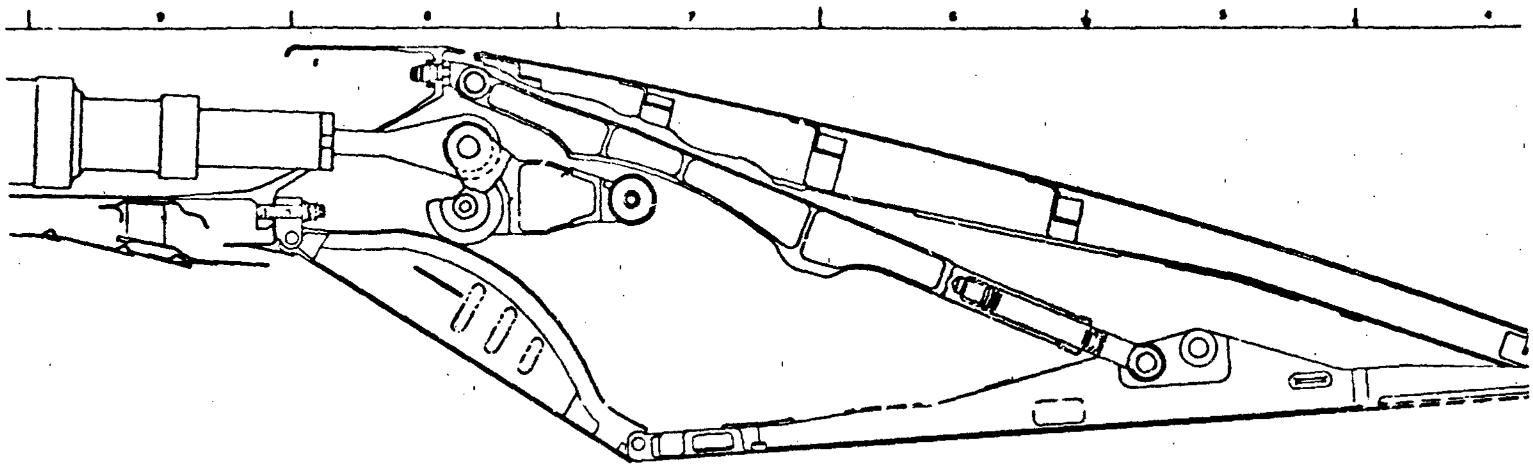

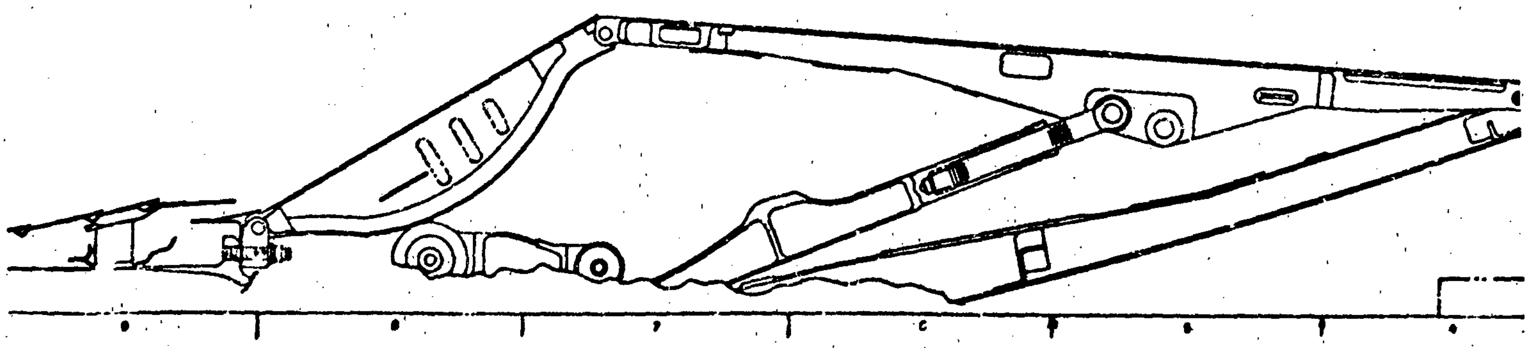

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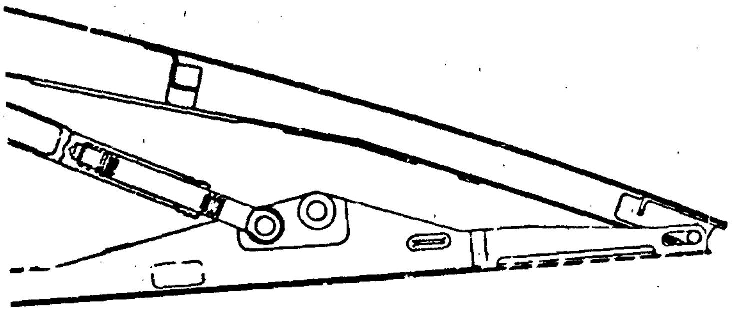
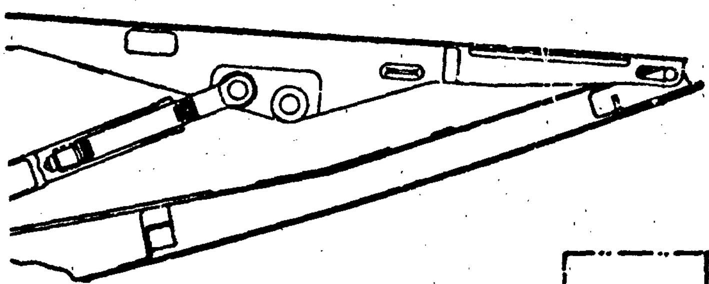




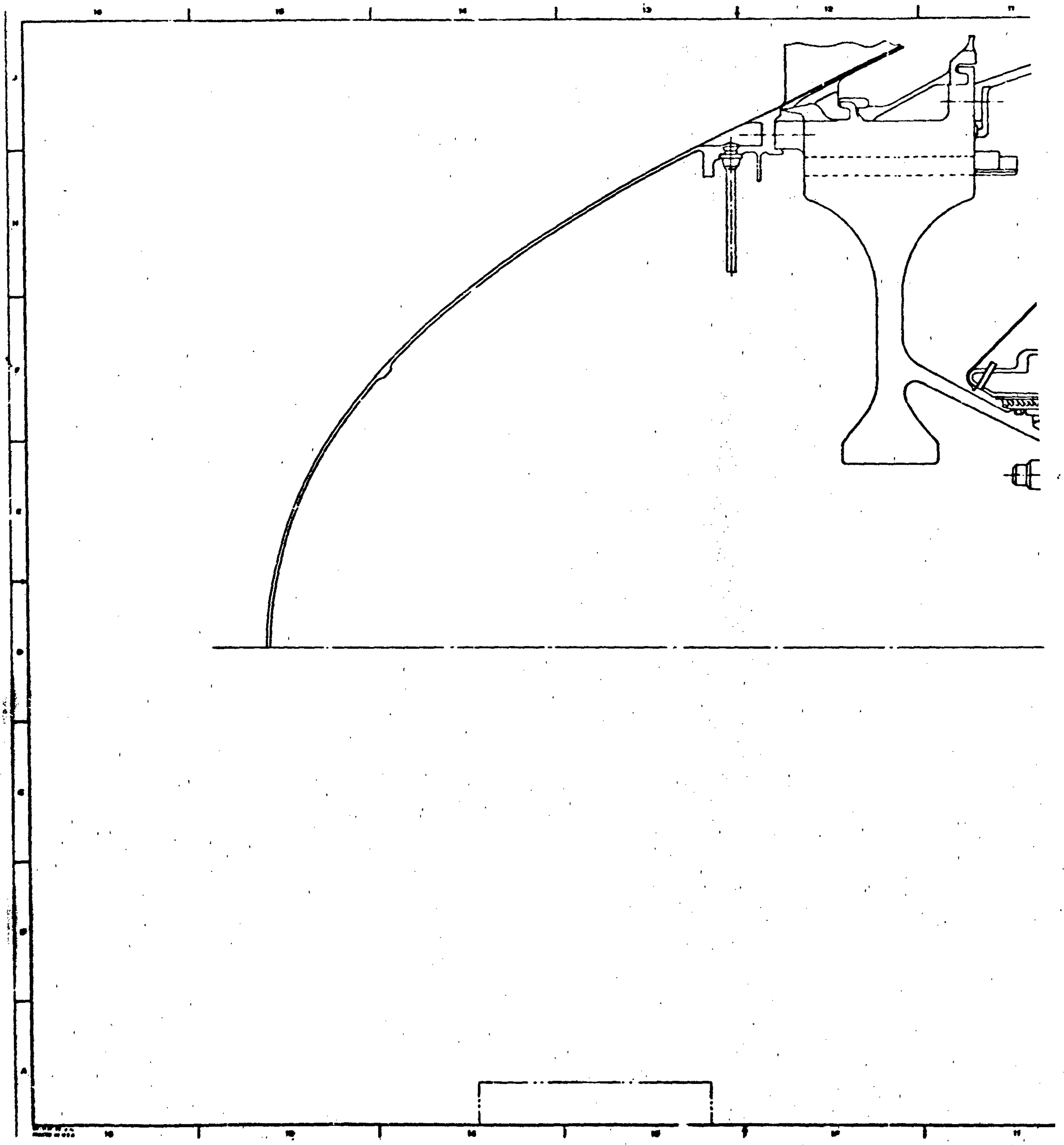
4013271-207

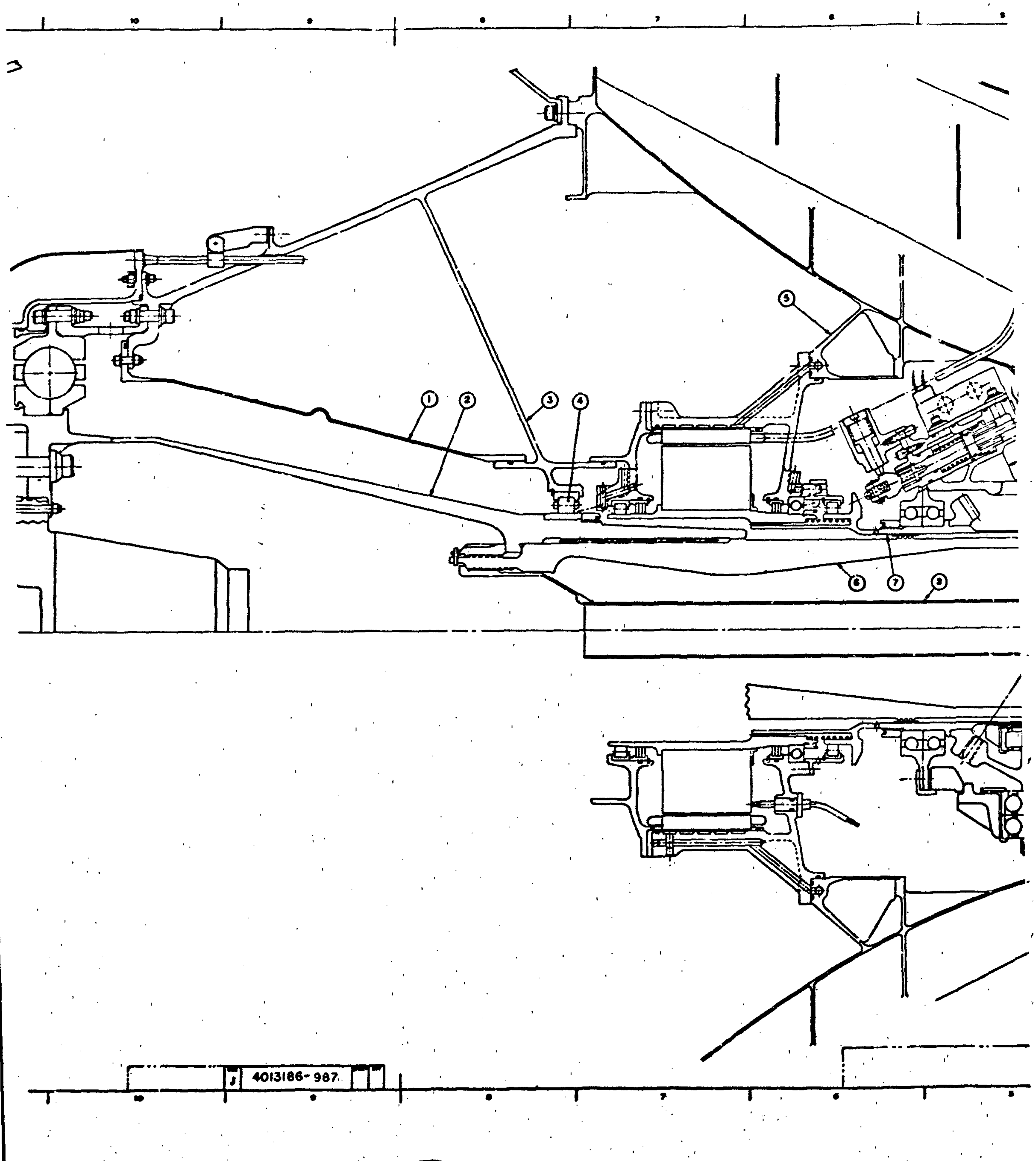
LAYOUT - PROPOSAL

GENERAL ELECTRIC

INTEGRATED ENGINE
STARTER / GENERATOR
200 KVA DISK (F404)

J 07482 4013271-207





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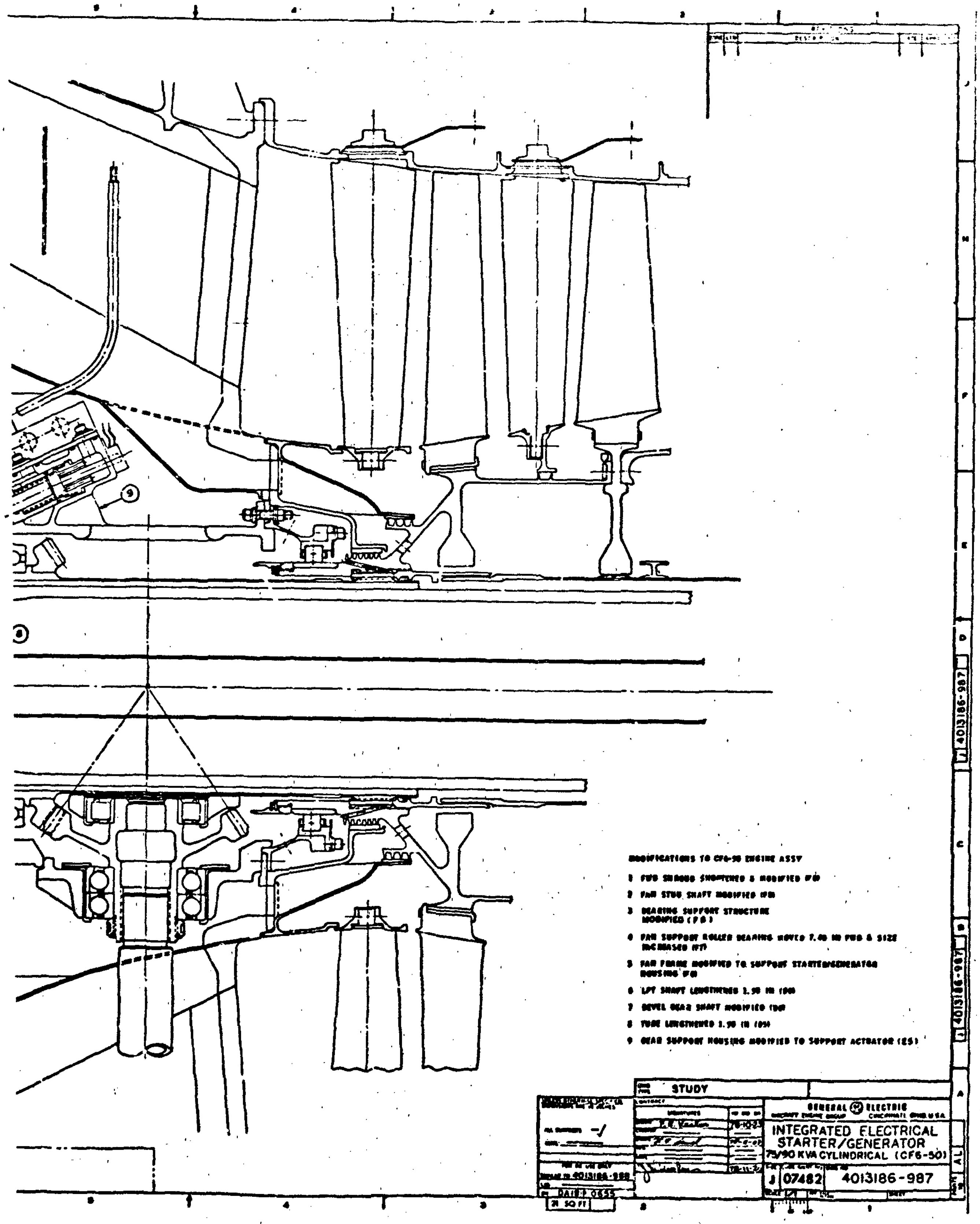
MODIFICATIONS TO CF6-50 ENGINE ASSY
1 FWD SHROUD SHORTENED & MODIFIED
2 FAN STUB SHAFT MODIFIED
3 BEARING SUPPORT STRUCTURE MODIFIED
4 FAN SUPPORT ROLLER BEARING MOVED 7.40 IN FWD & SIZE INCREASED
5 FAN FRAME MODIFIED TO SUPPORT STARTER/GENERATOR HOUSING
6 LPT SHAFT LENGTHENED 1.50 IN
7 BEVEL GEAR SHAFT MODIFIED
8 TUBE LENGTHENED 1.50 IN
9 GEAR SUPPORT HOUSING MODIFIED TO SUPPORT ACTUATOR (E5)
STUDY
GENERAL ELECTRIC
INTEGRATED ELECTRICAL STARTER/GENERATOR
75/90 KVA CYLINDRICAL (CF6-50)
J 07482 4013186-987
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DA18-P 0655
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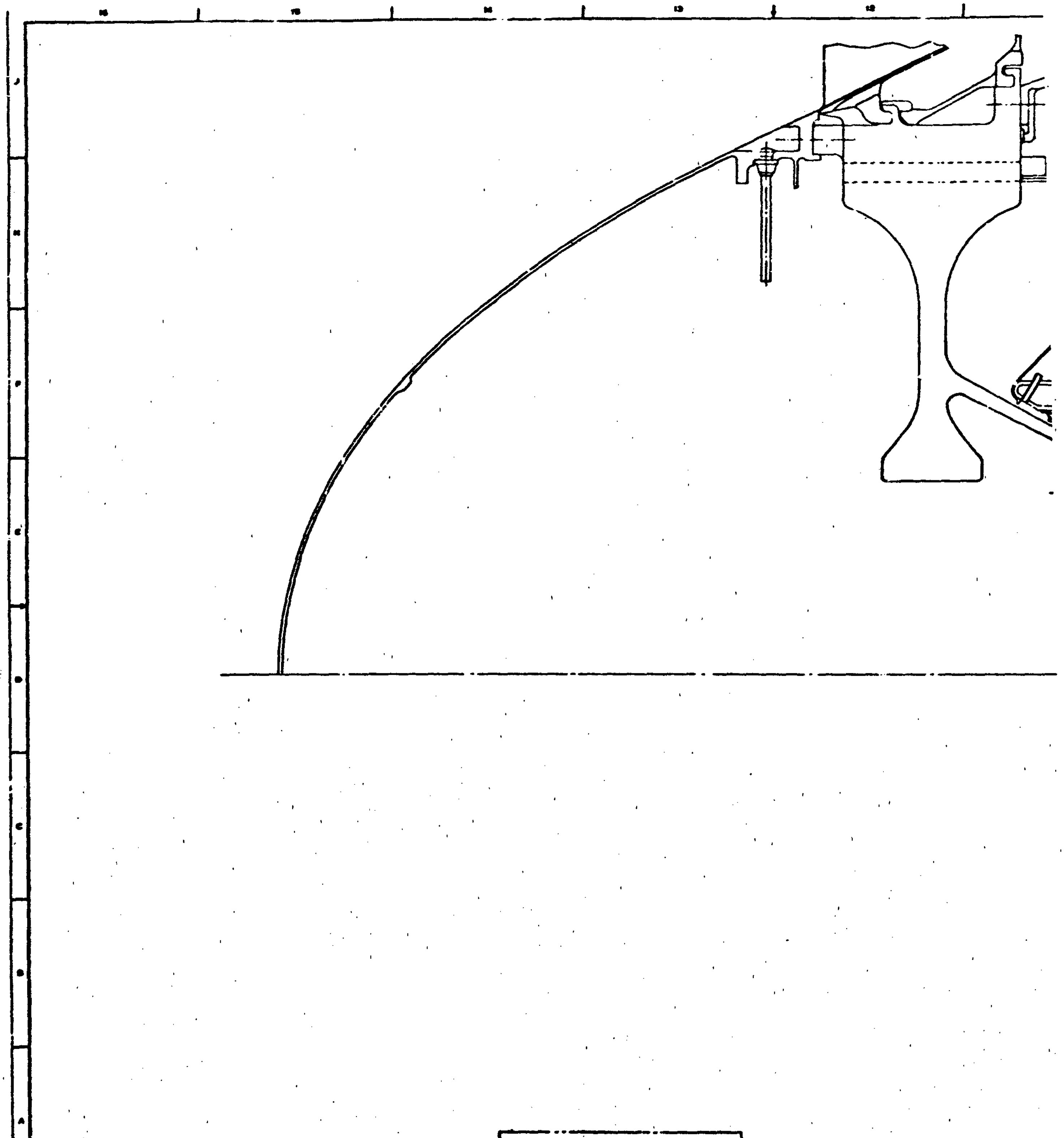

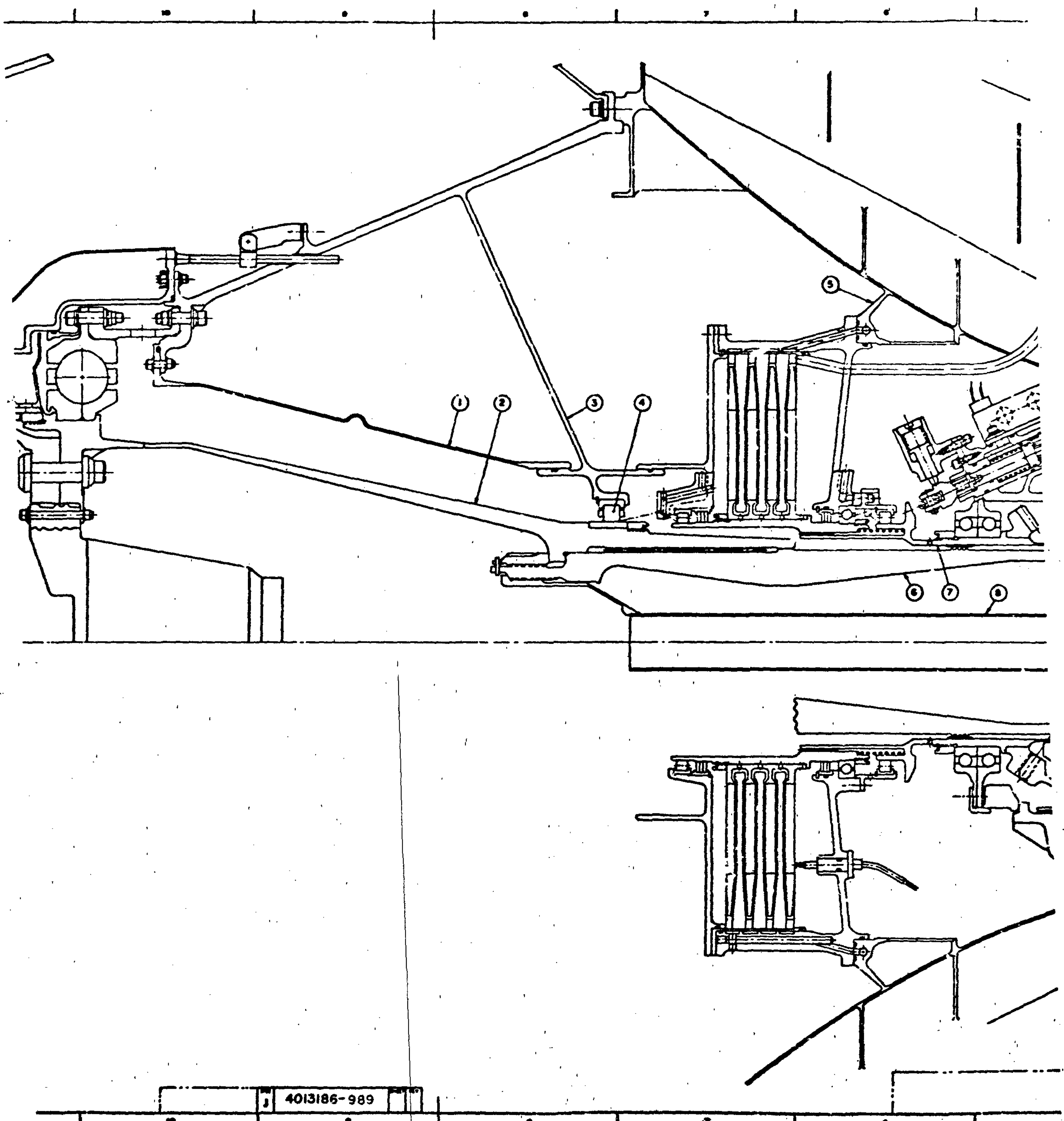

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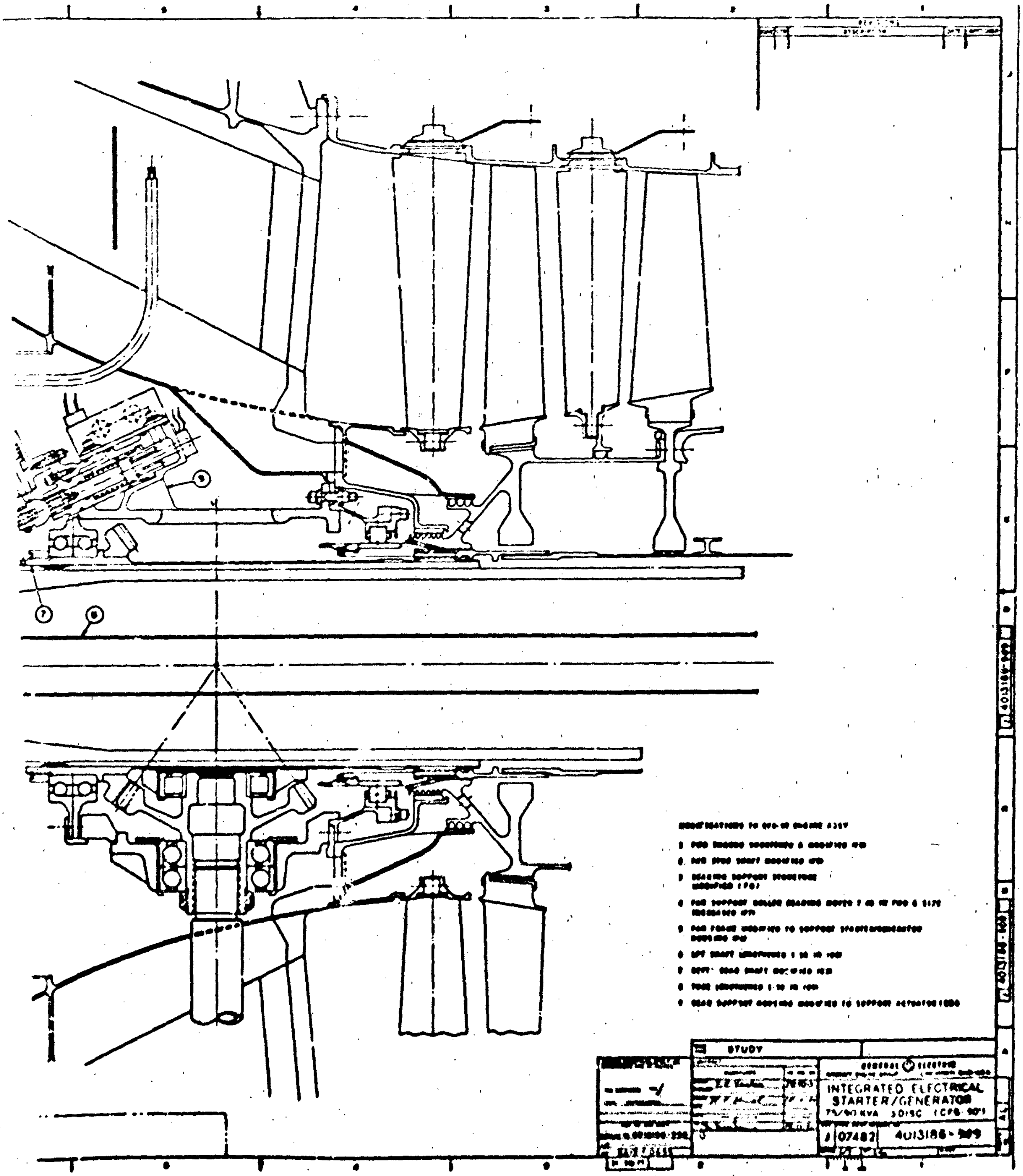
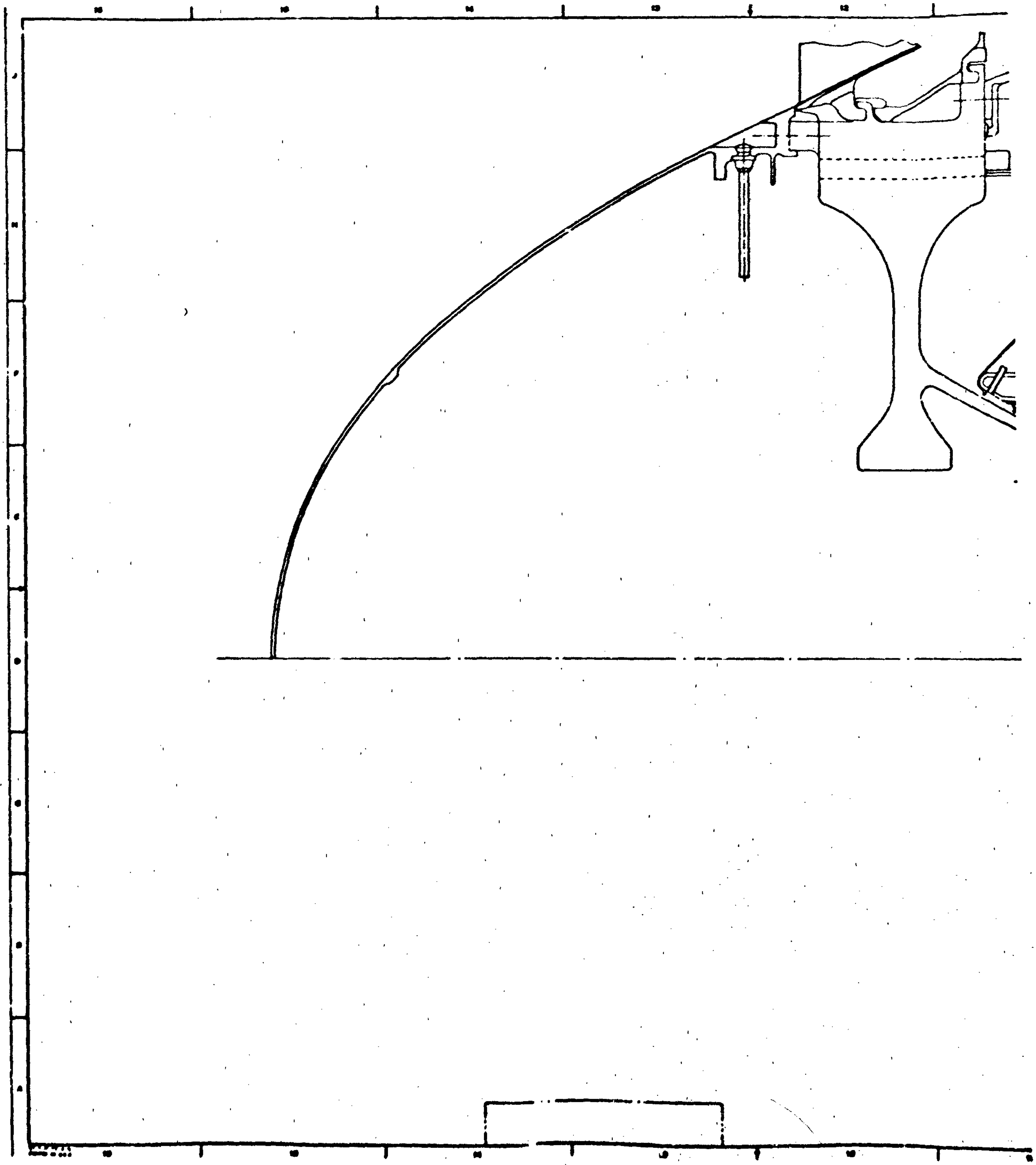
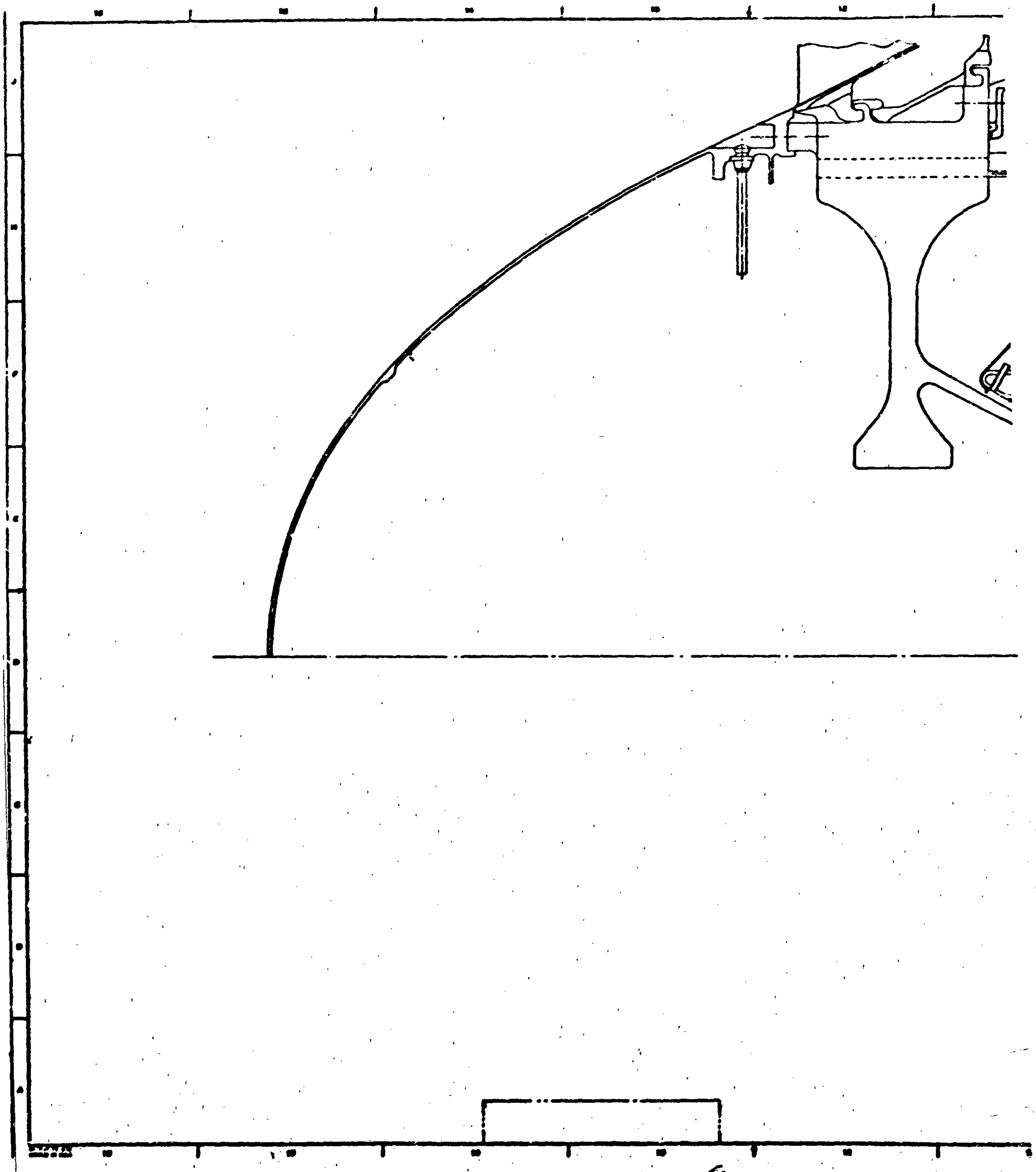
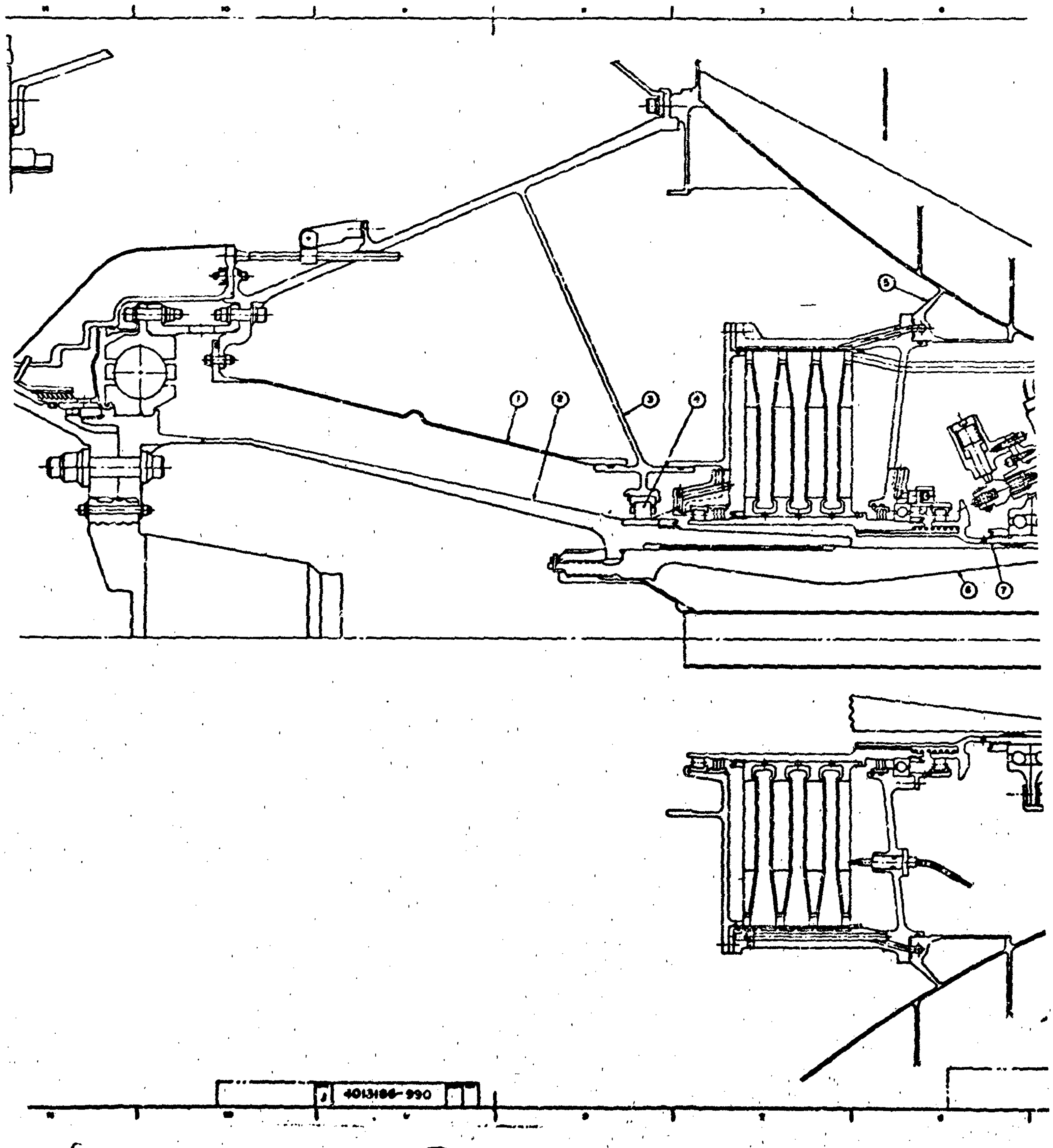

STUDY
INTEGRATED ELECTRICAL
STARTER/GENERATOR
07482



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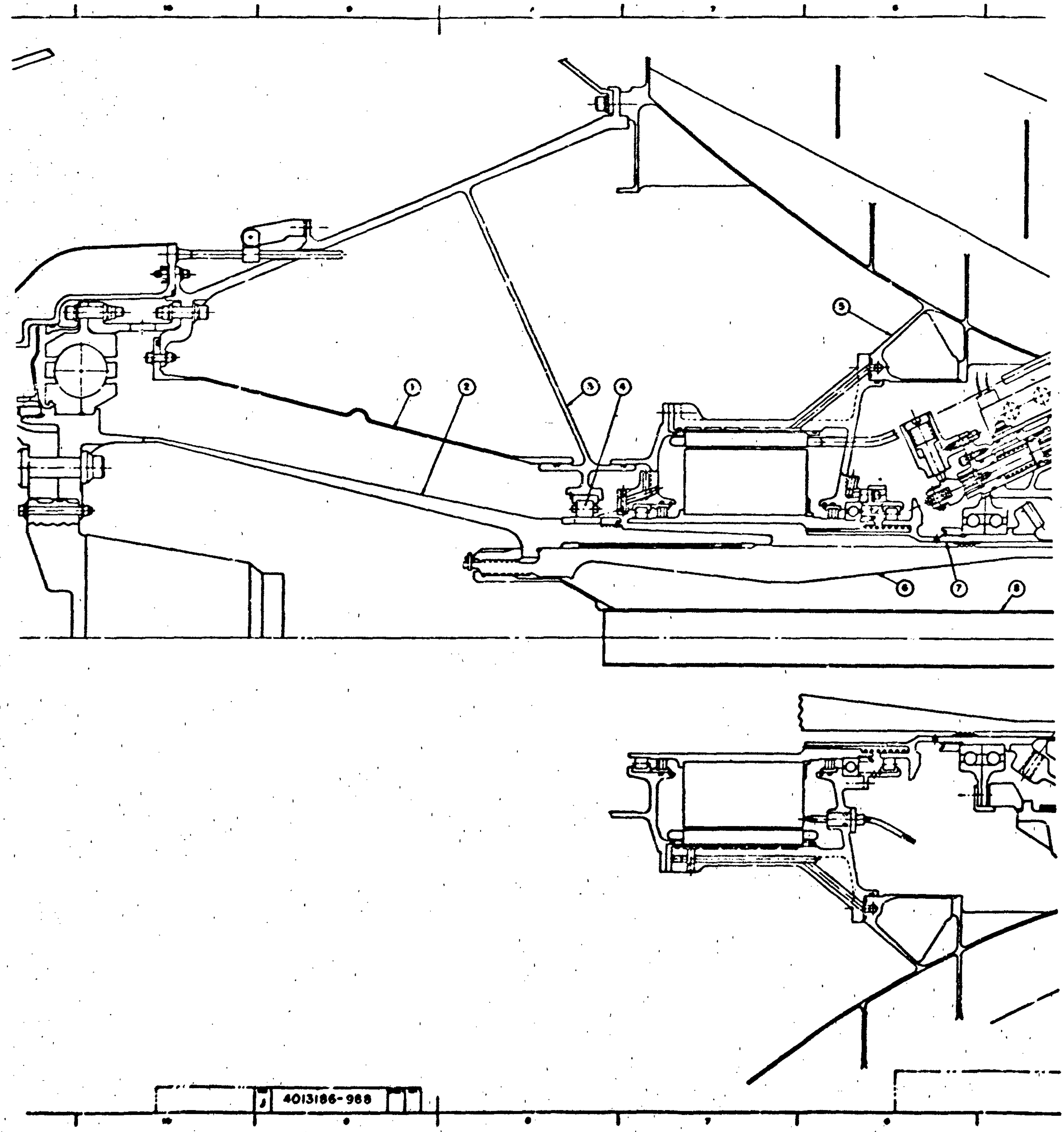

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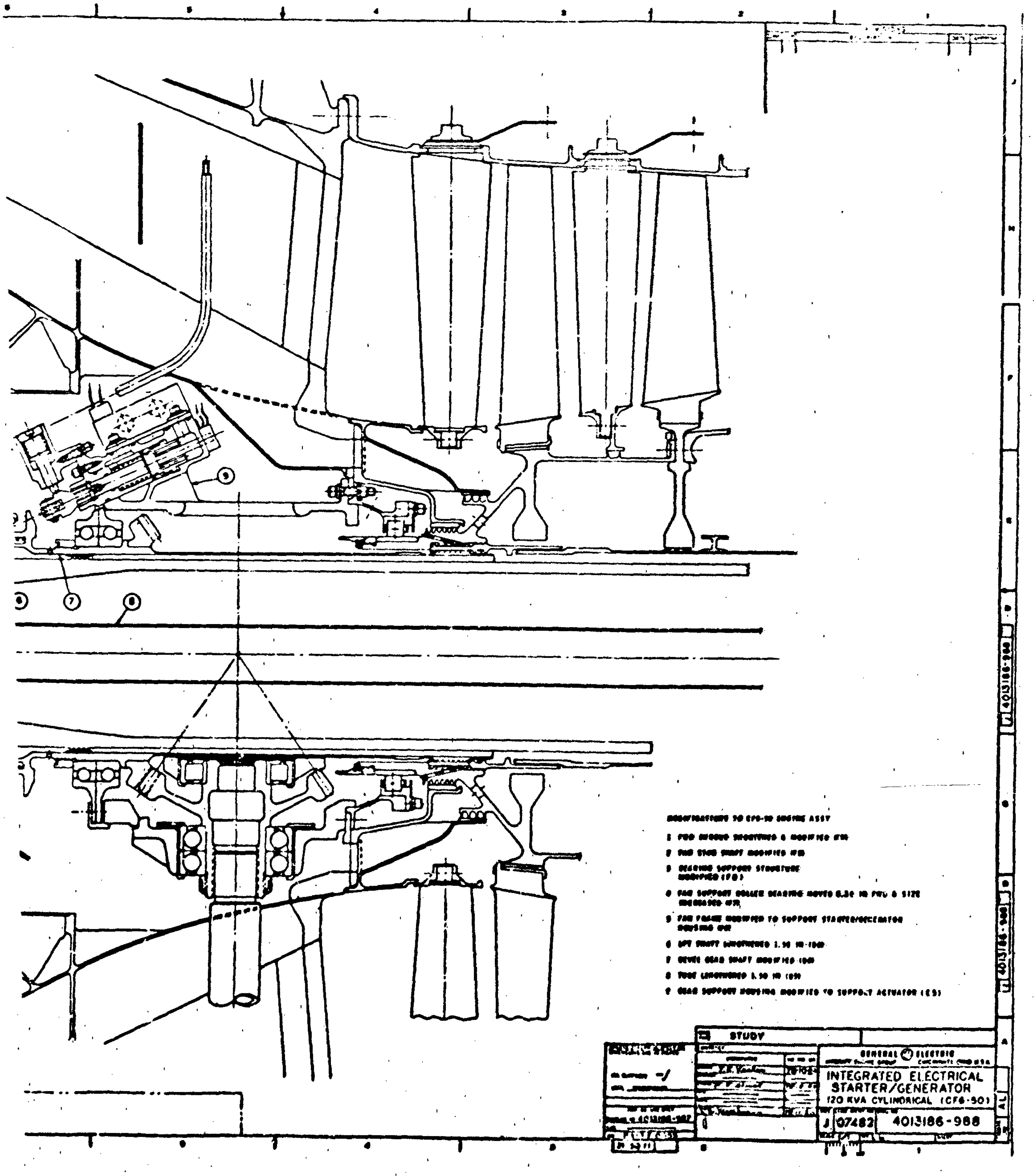
STUDY
GENERAL ELECTRIC
INTEGRATED ELECTRICAL
STARTER/GENERATOR
120 KVA CYLINDRICAL (CF6-50)
J 07482
4013186-988

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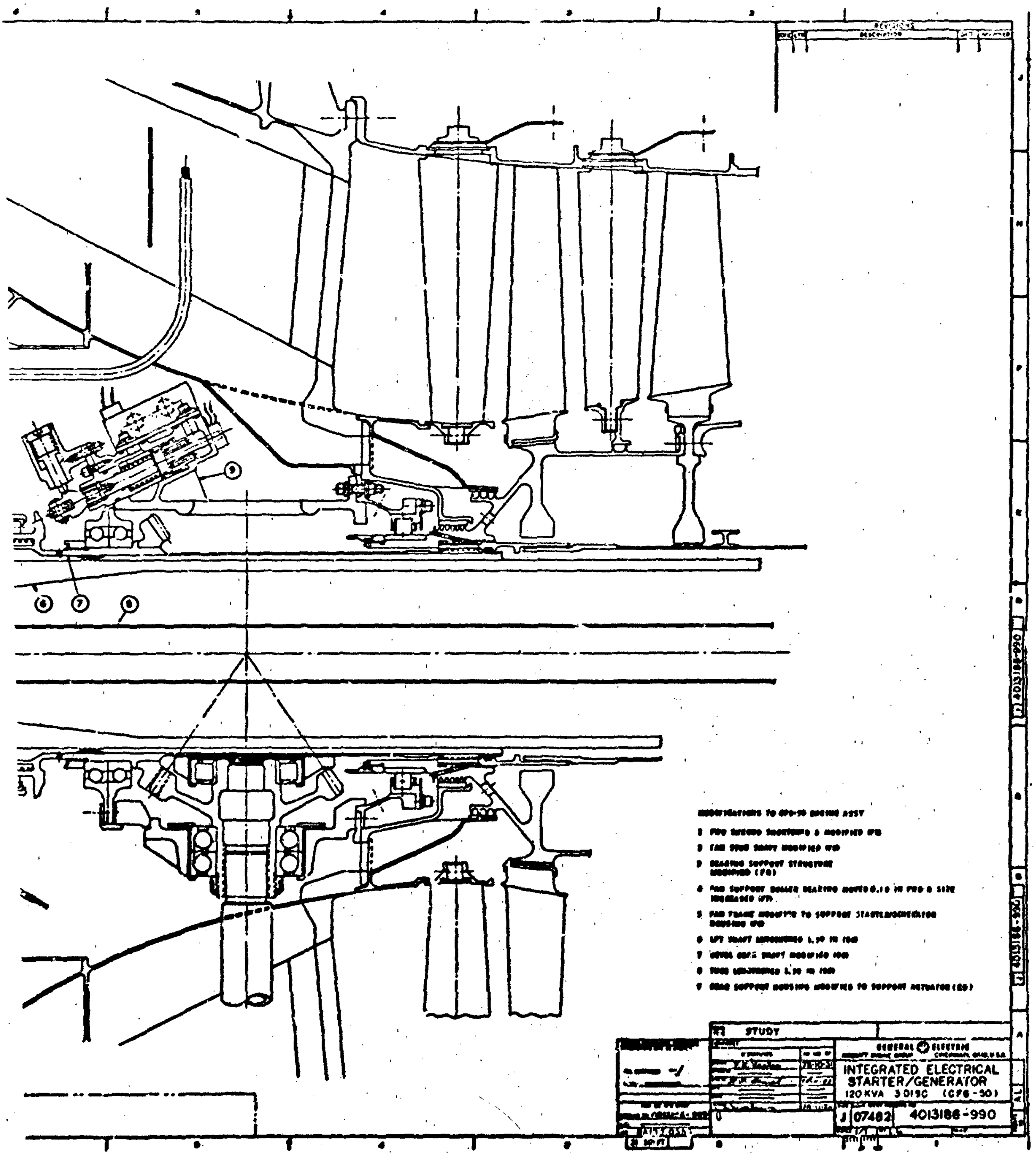
REVISIONS
DESCRIPTION
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STUDY
GENERAL ELECTRIC
INTEGRATED ELECTRICAL
STARTER/GENERATOR
120 KVA 3 DISC (CF6-50)
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4013186-990

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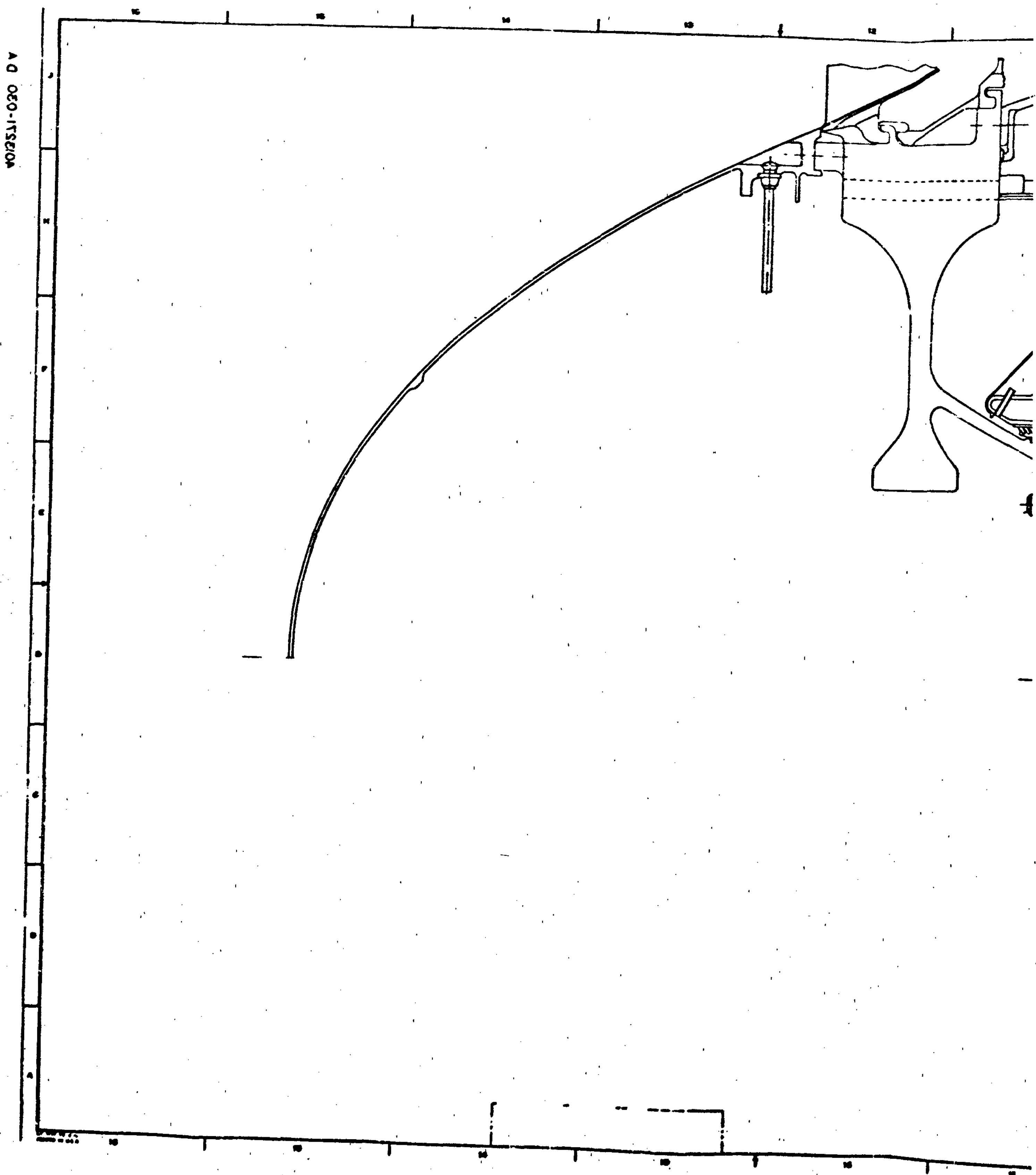

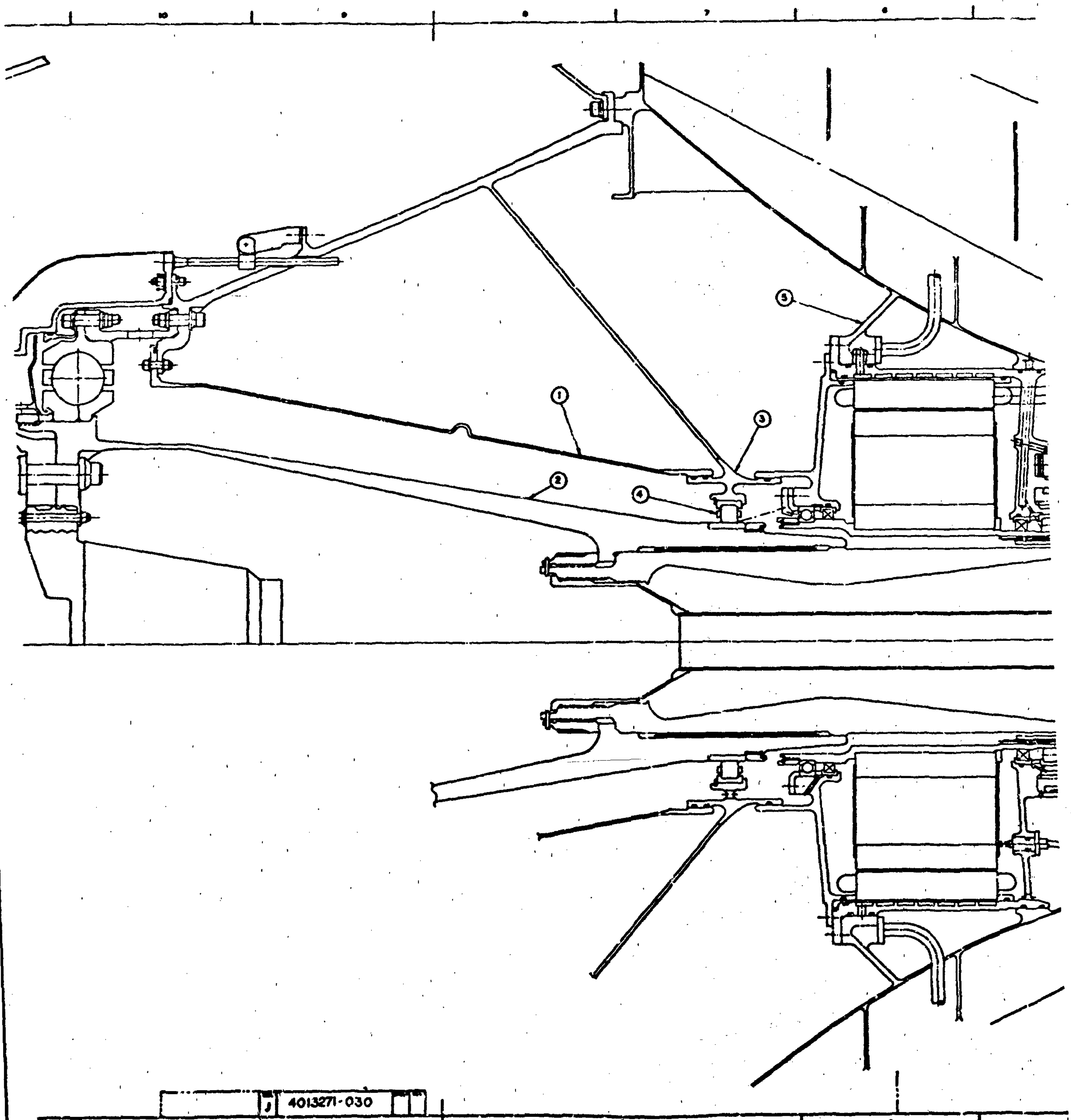

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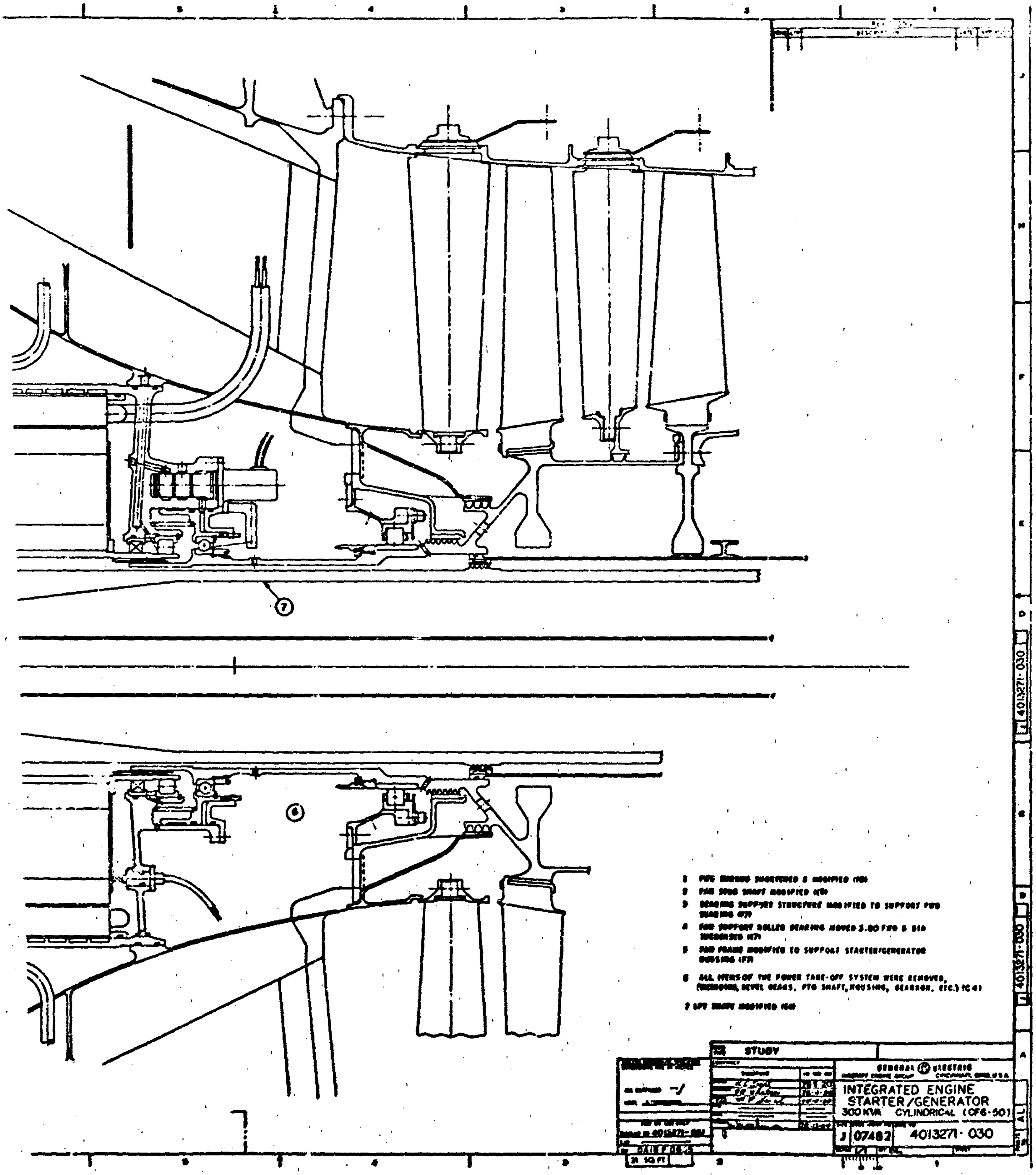
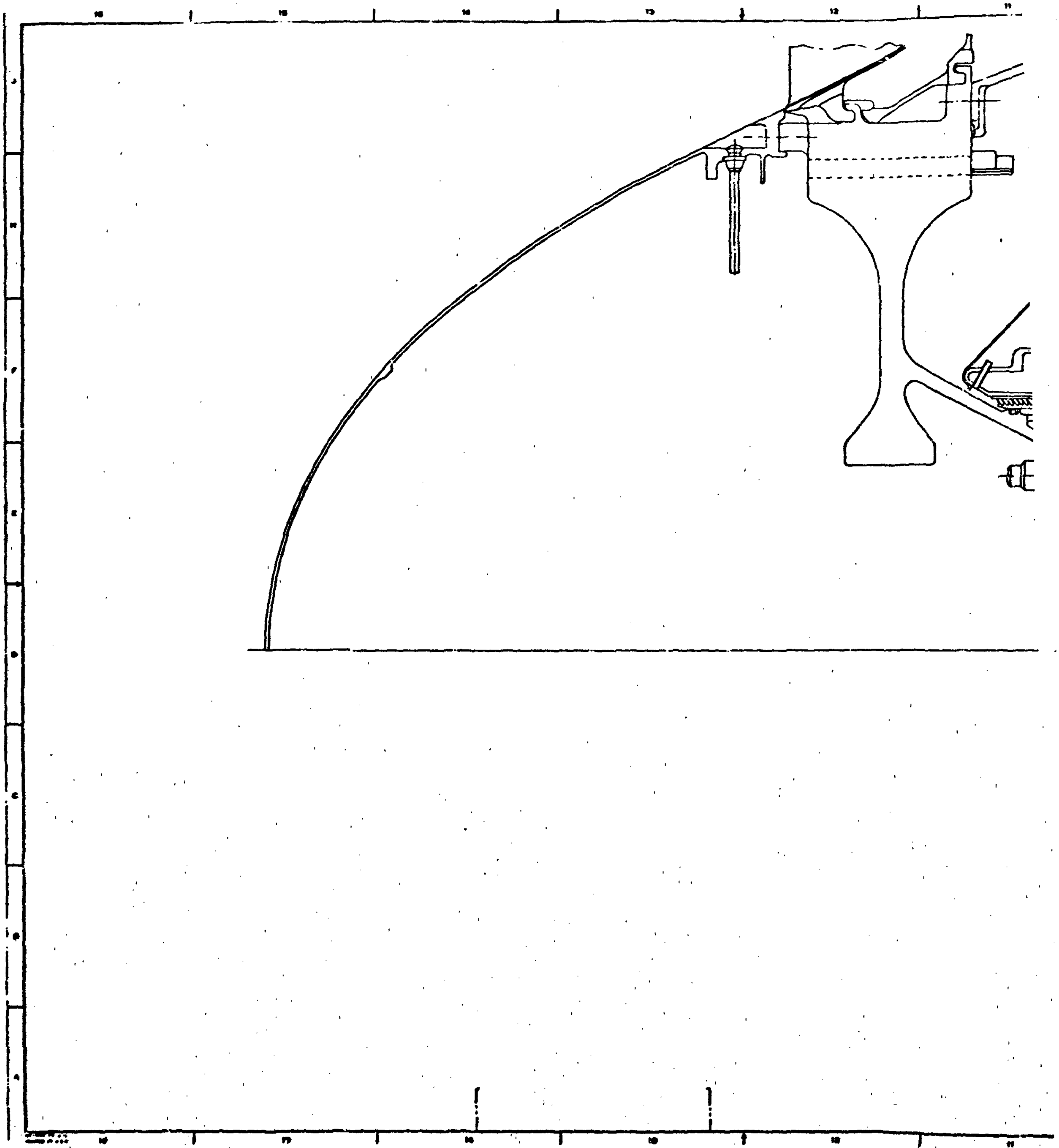


1 FWD SHROUD SHORTENED & MODIFIED (F8)
2 FAN STUB SHAFT MODIFIED (E8)
3 BEARING SUPPORT STRUCTURE MODIFIED TO SUPPORT FWD BEARING (F7)
4 FAN SUPPORT ROLLER BEARING MOVED 3.00 FWD & DIA INCREASED (E7)
5 FAN FRAME MODIFIED TO SUPPORT STARTER/GENERATOR HOUSING (F7)
6 ALL ITEMS OF THE POWER TAKE-OFF SYSTEM WERE REMOVED, (INCLUDING BEVEL GEARS, PTO SHAFT, HOUSING, GEARBOX, ETC.) (C4)
7 LPT SHAFT MODIFIED (G4)
STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE STARTER/GENERATOR
300 KVA CYLINDRICAL (CF6-50)
J 07482
4013271-030



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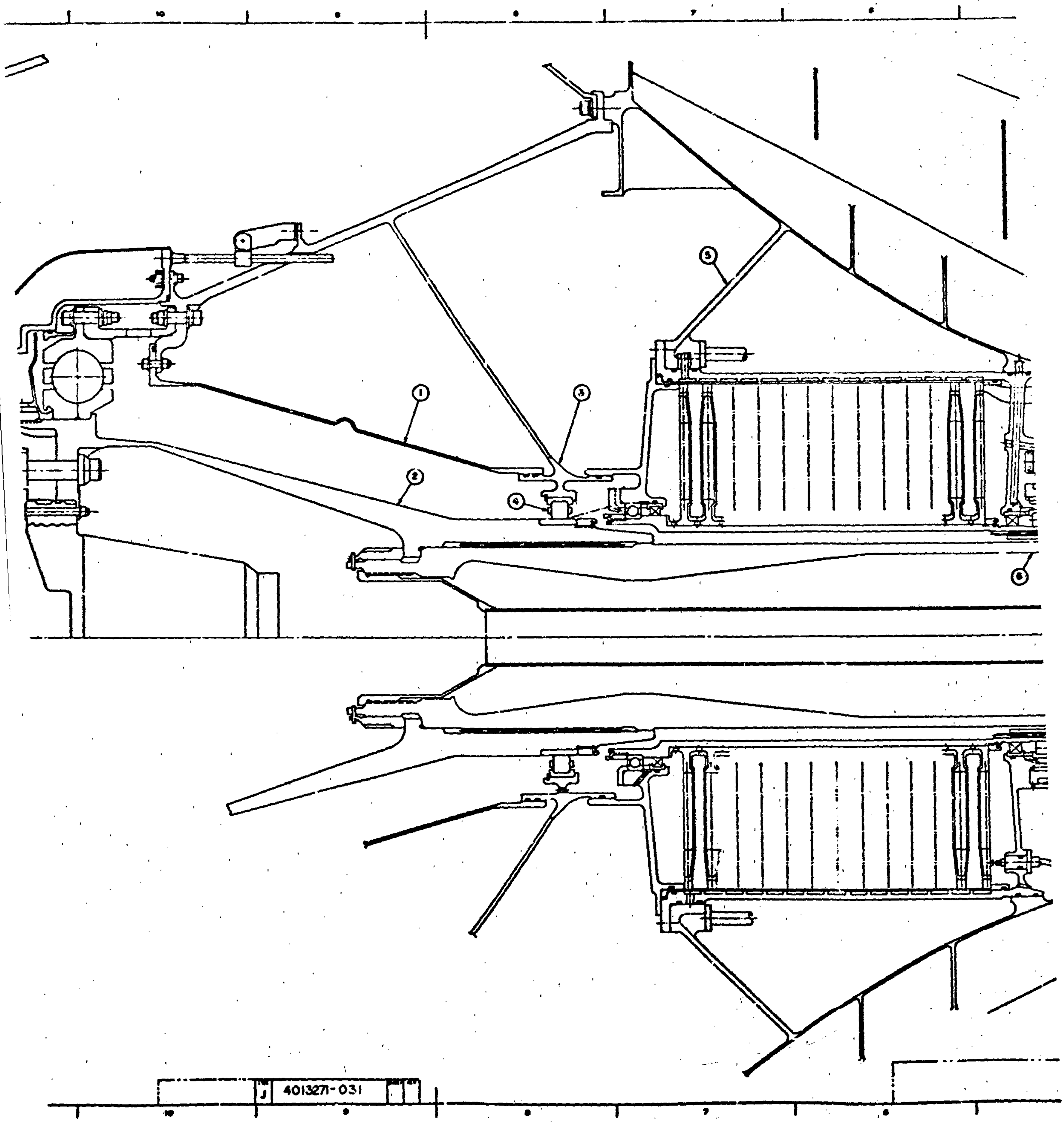
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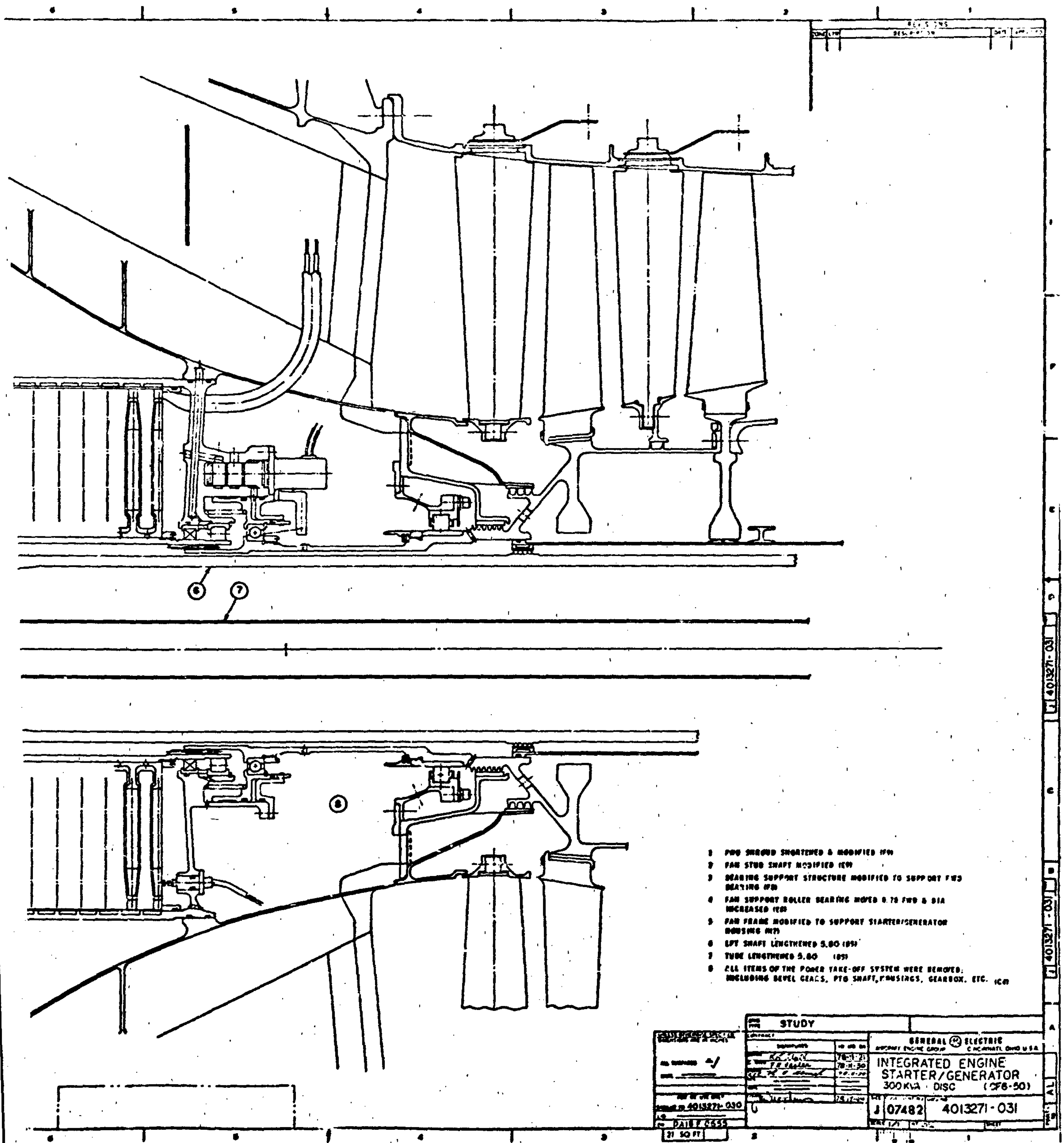

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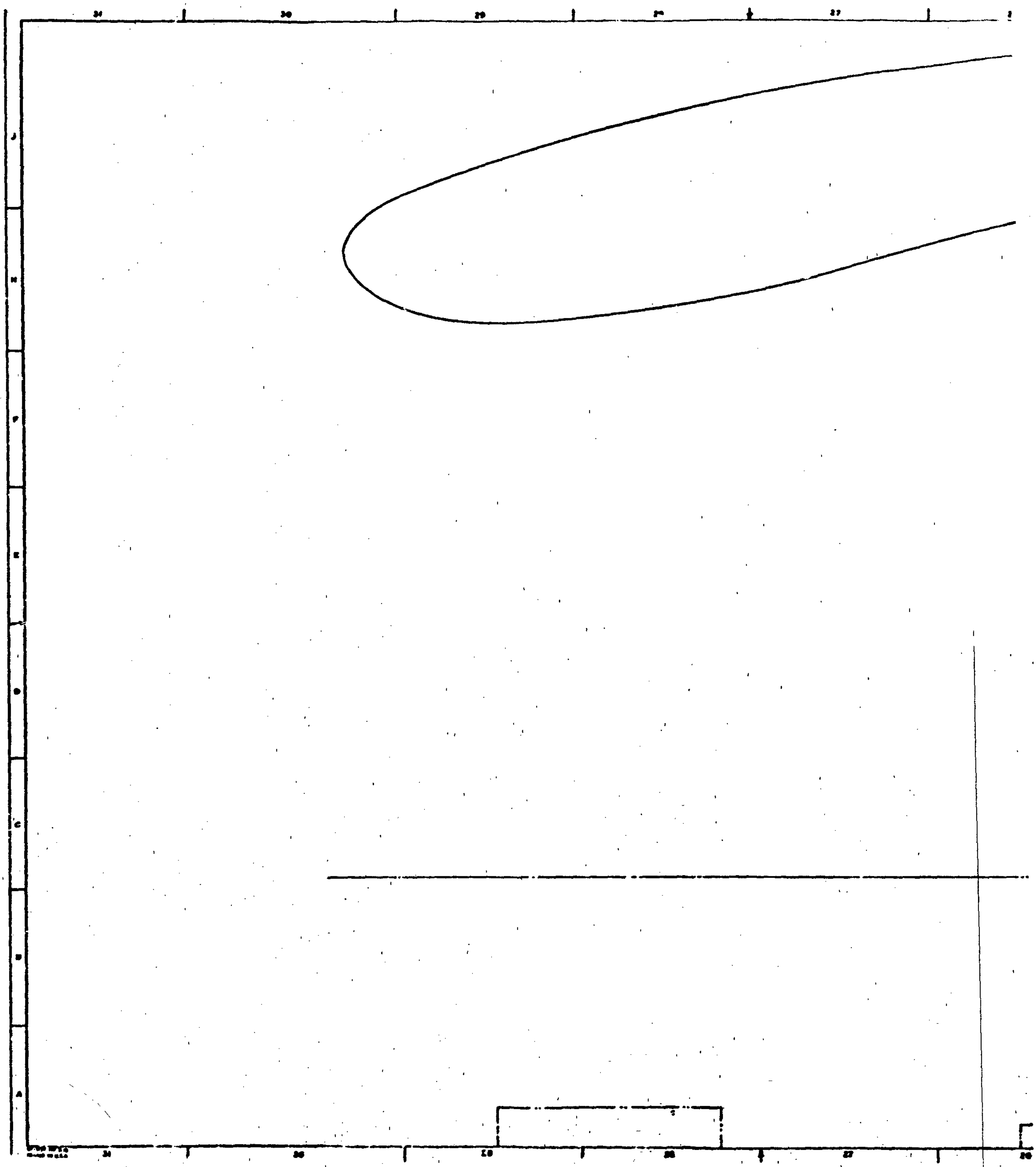
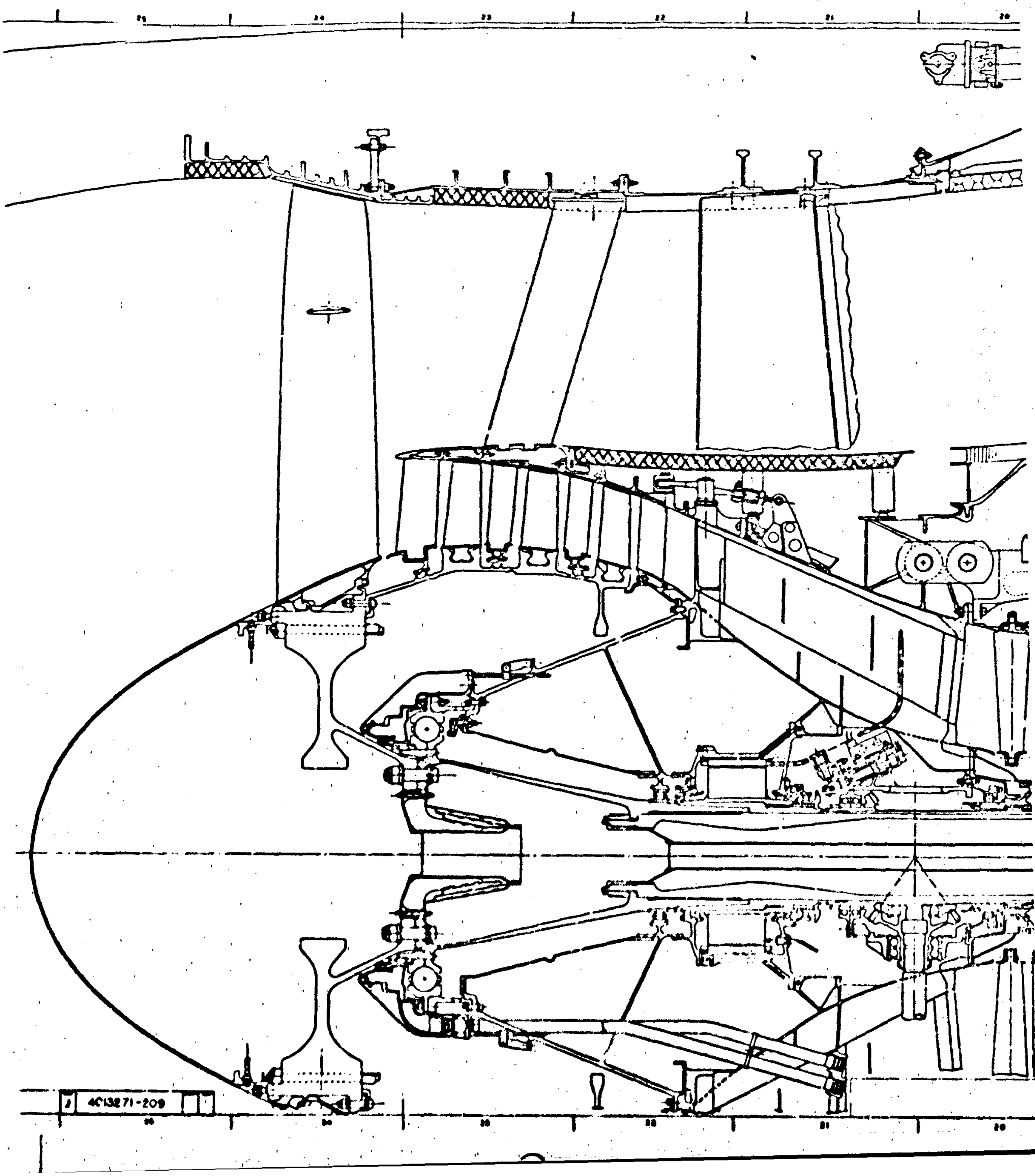
1 FWD SHROUD SHORTENED & MODIFIED (F9)
2 FAN STUB SHAFT MODIFIED (E9)
3 BEARING SUPPORT STRUCTURE MODIFIED TO SUPPORT FWD BEARING (F8)
4 FAN SUPPORT ROLLER BEARING MOVED 0.75 FWD & DIA INCREASED (E8)
5 FAN FRAME MODIFIED TO SUPPORT STARTER/GENERATOR HOUSING (H7)
6 LPT SHAFT LENGTHENED 5.80 (D9)
7 TUBE LENGTHENED 5.80 (D9)
8 ALL ITEMS OF THE POWER TAKE-OFF SYSTEM WERE REMOVED: INCLUDING BEVEL GEARS, PTO SHAFT, HOUSINGS, GEARBOX, ETC. (C7)
STUDY
GENERAL ELECTRIC
AIRCRAFT ENGINE GROUP
CINCINNATI, OHIO U.S.A.
INTEGRATED ENGINE
STARTER/GENERATOR
300 KVA · DISC (CF6-50)
J 07482 4013271-031
4013271-030
4013271-031





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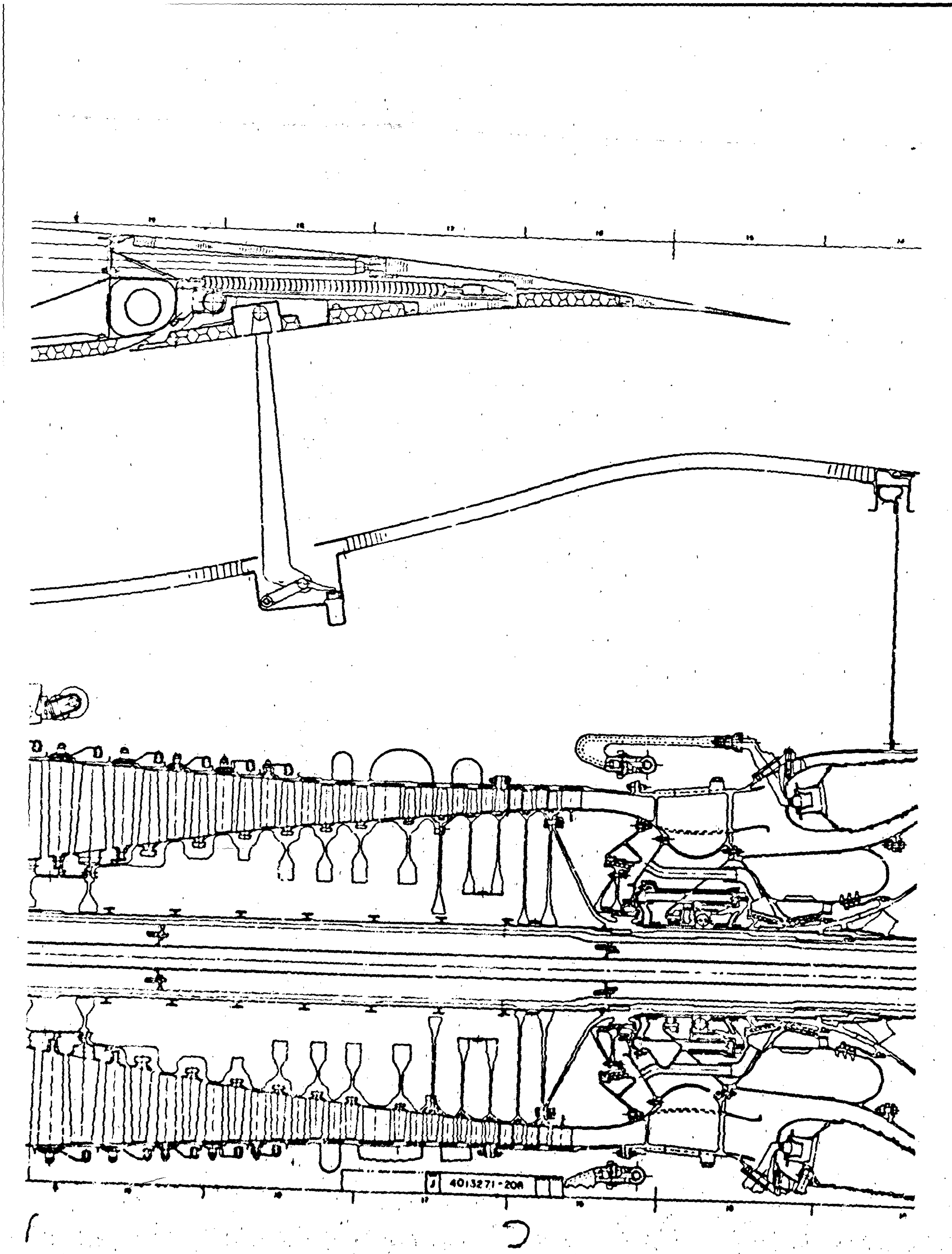

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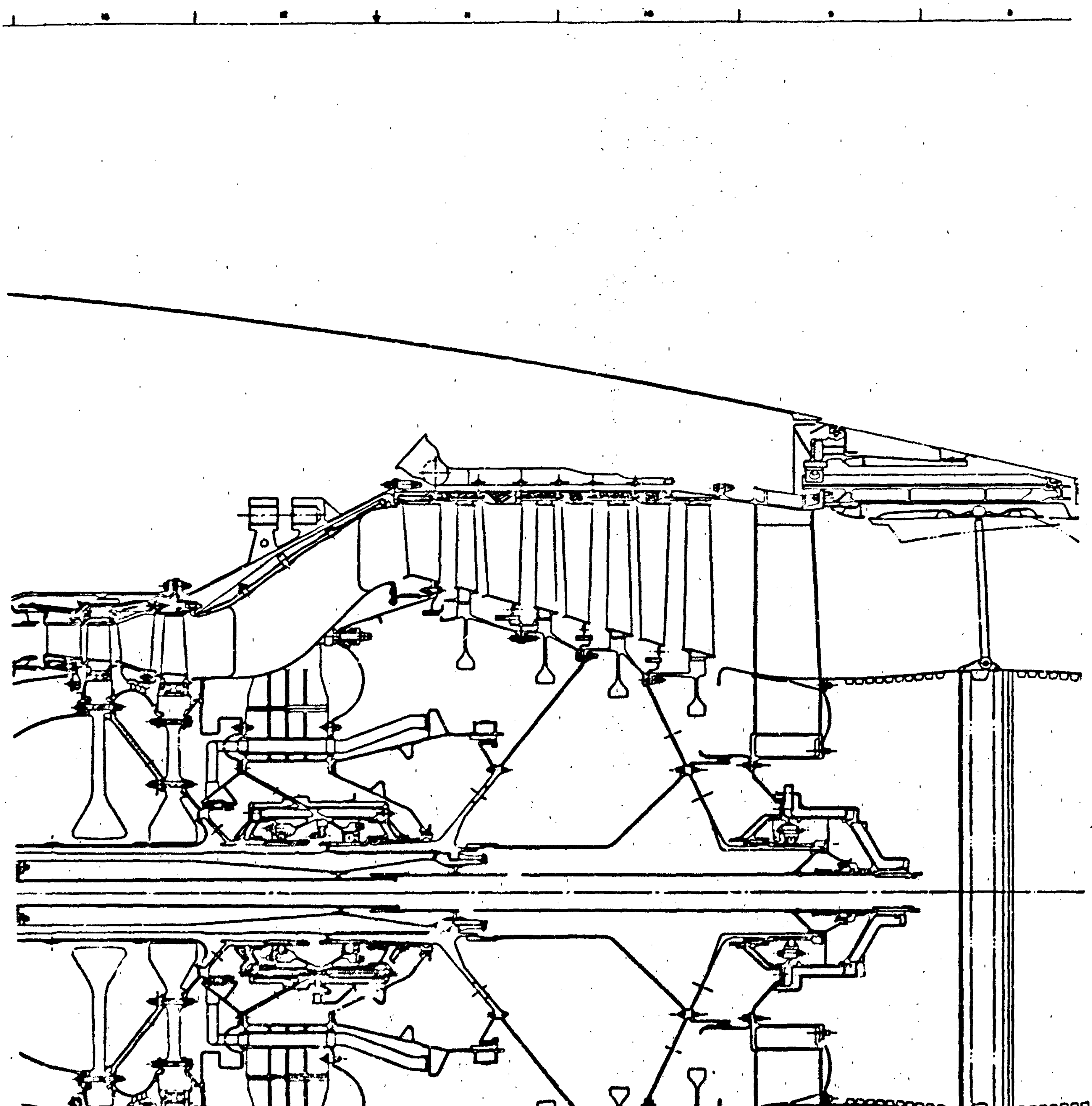

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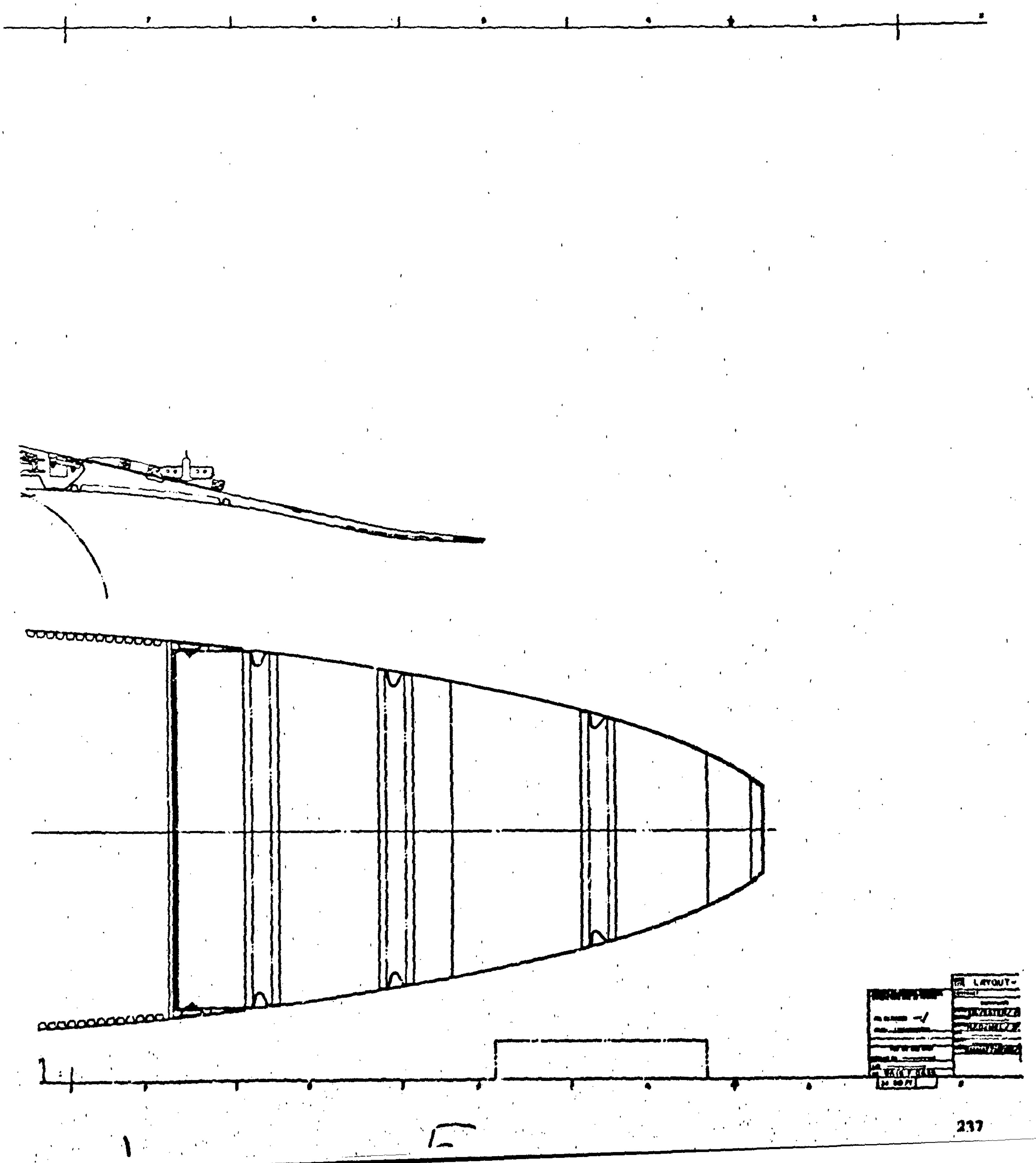
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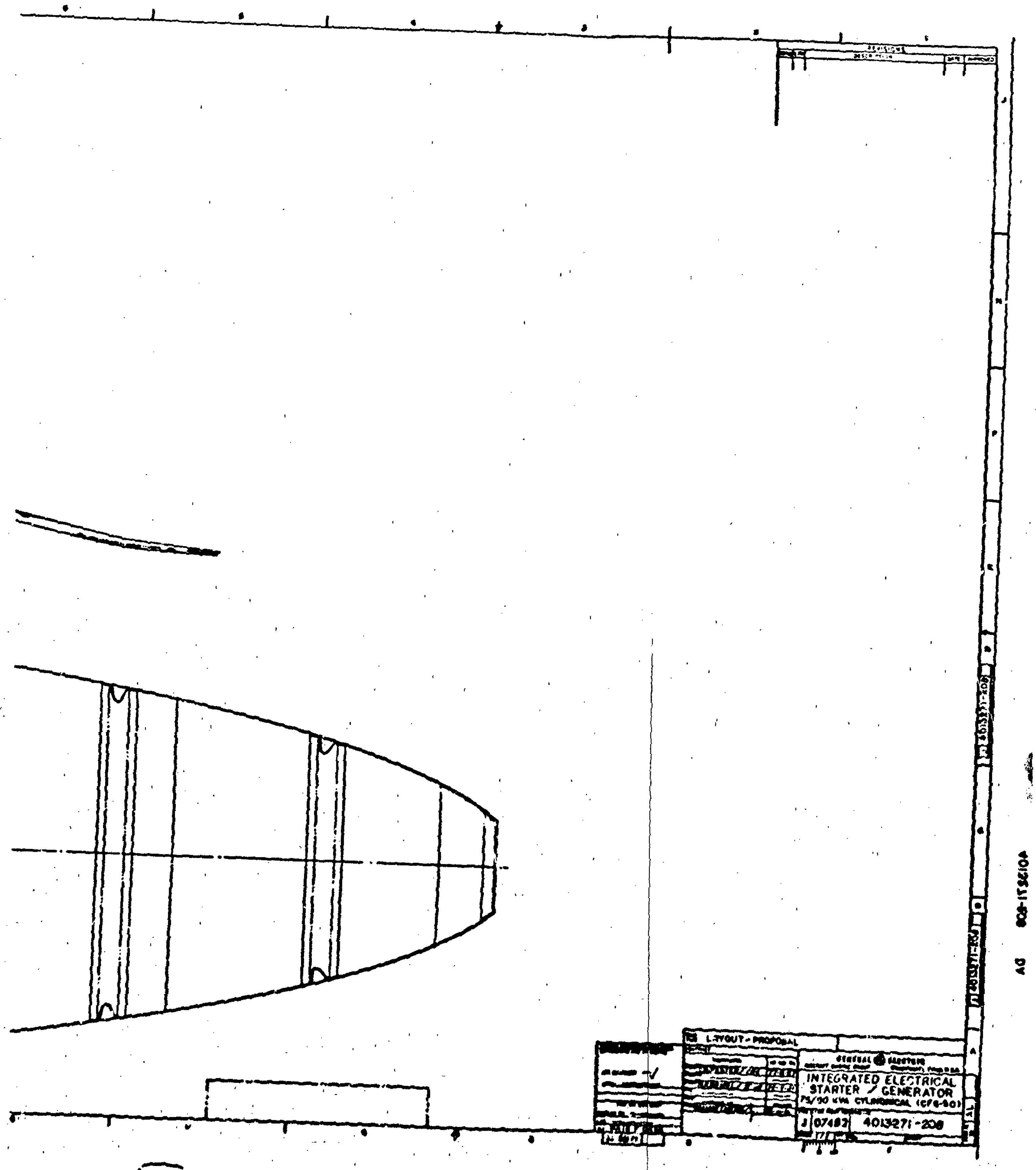
LAYOUT - PROPOSAL
GENERAL ELECTRIC
INTEGRATED ELECTRICAL
STARTER / GENERATOR
75/90 KVA CYLINDRICAL (CF6-50)
J
07482
4013271-208

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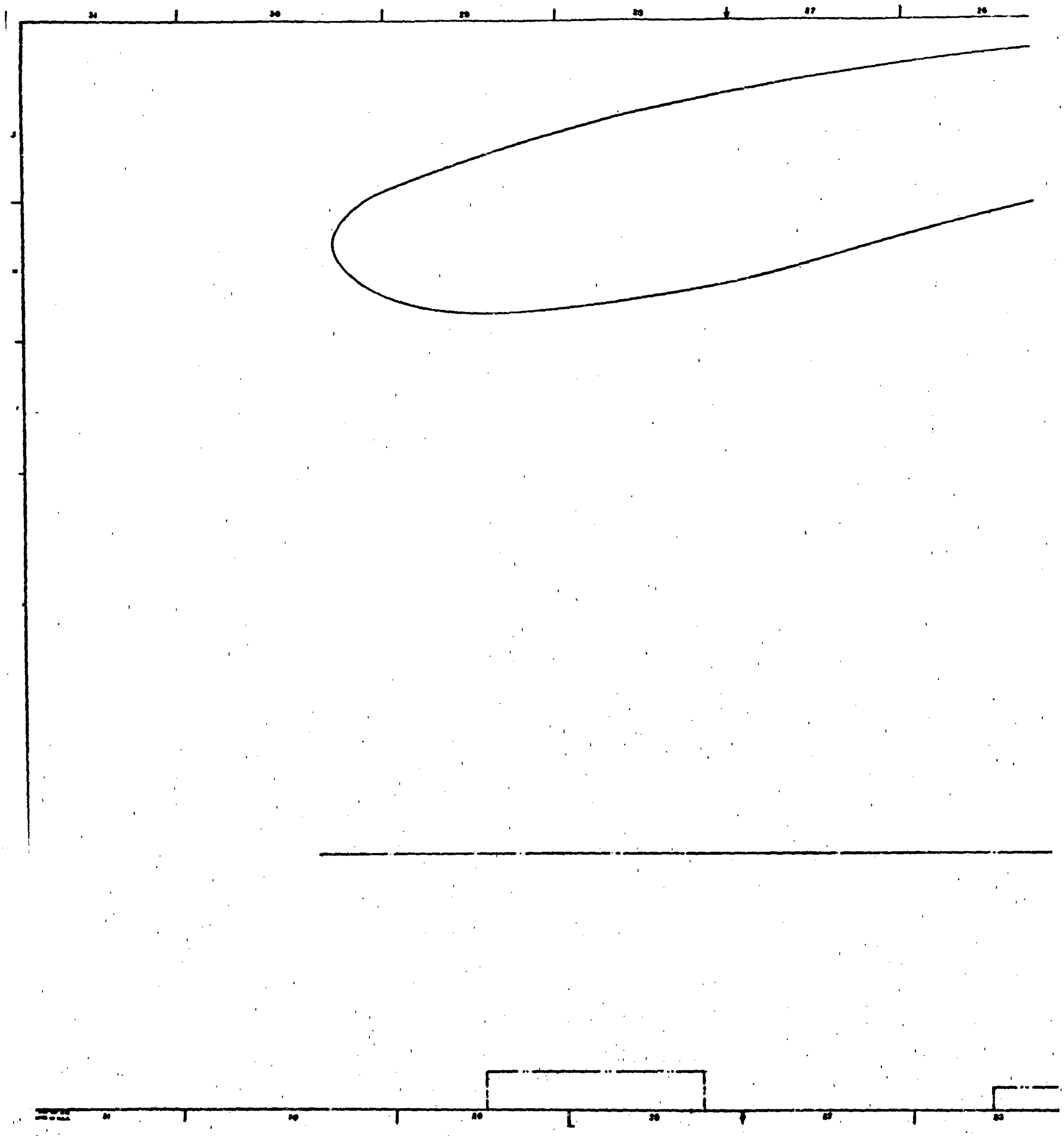

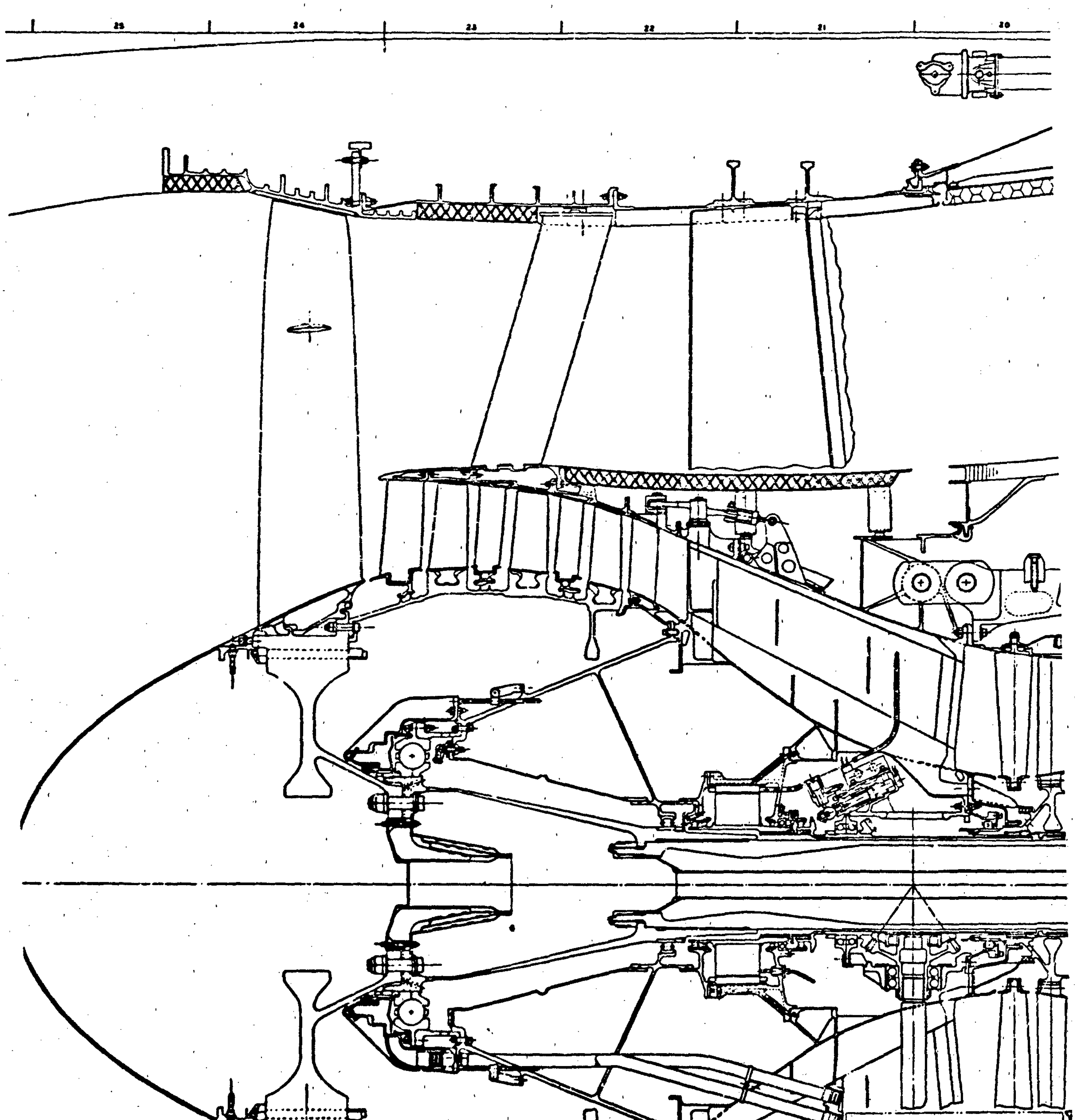

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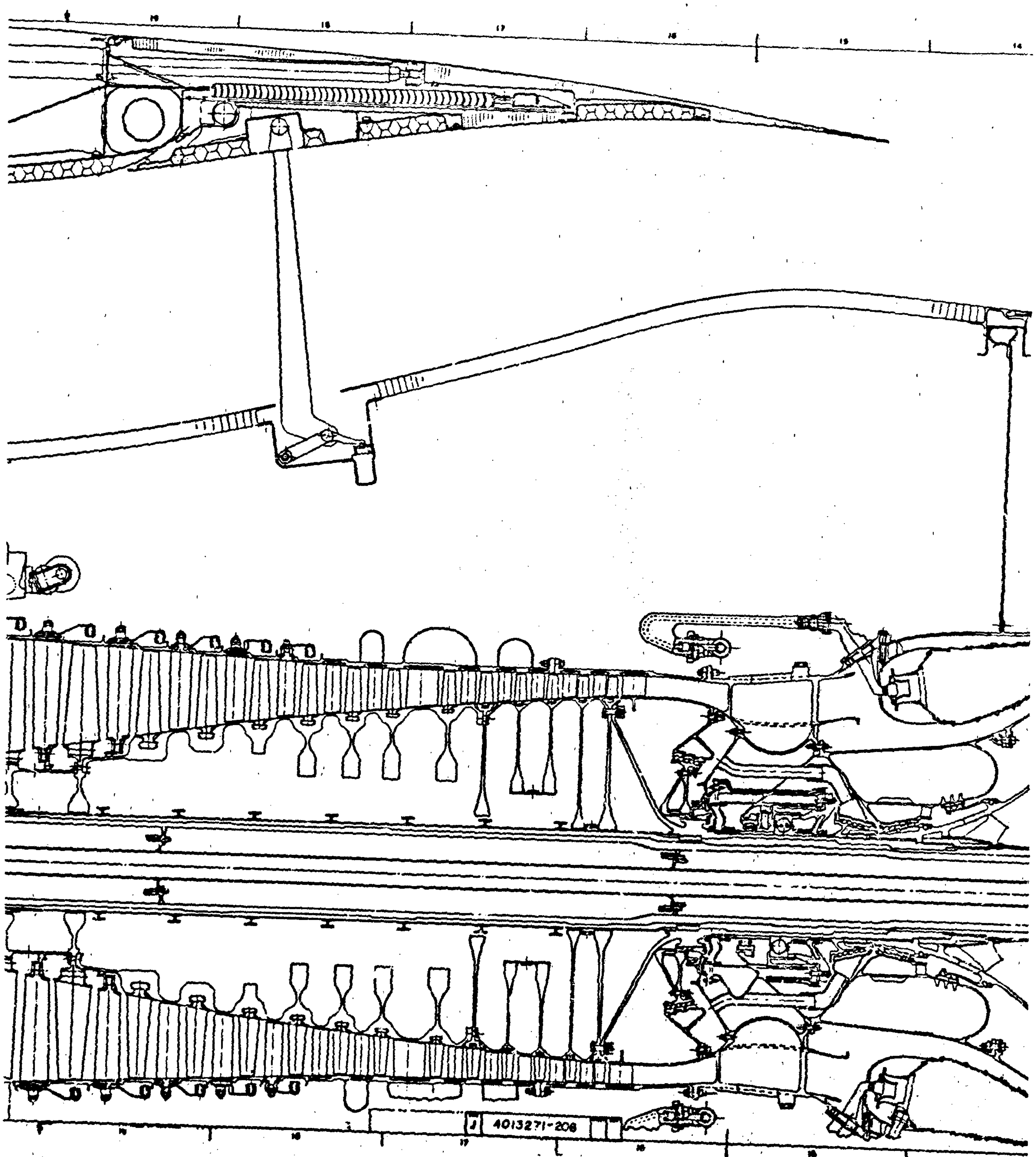

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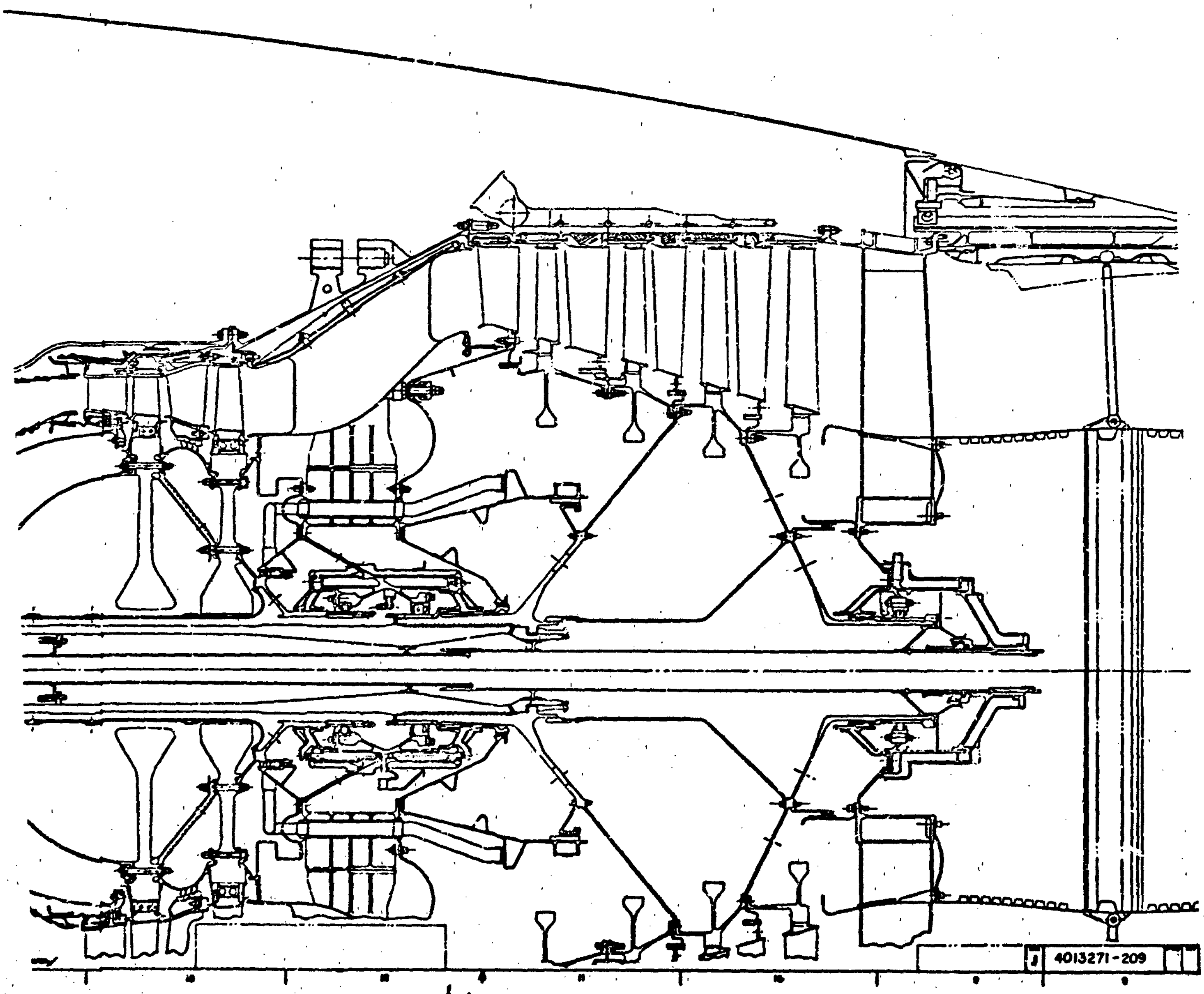
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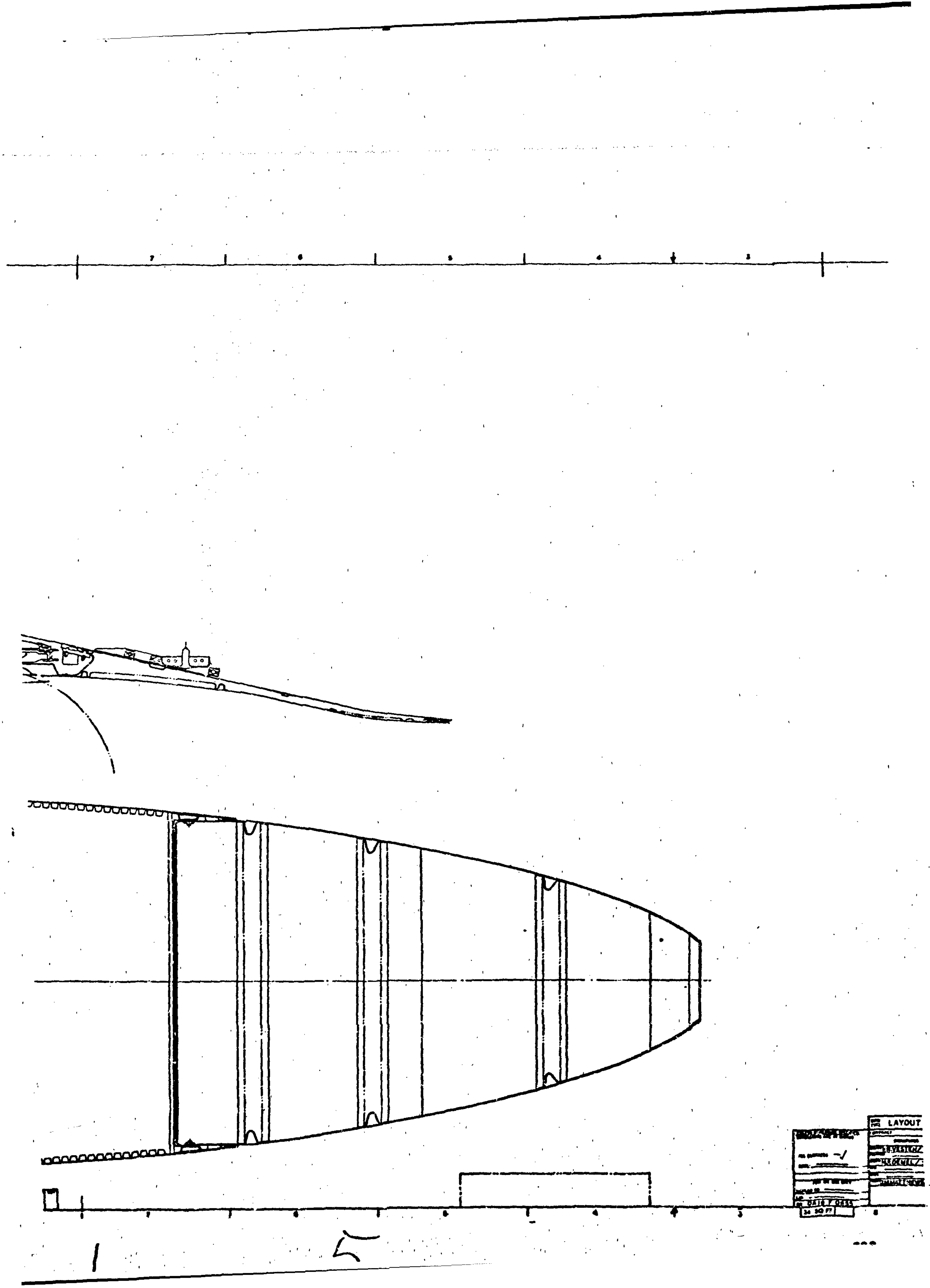
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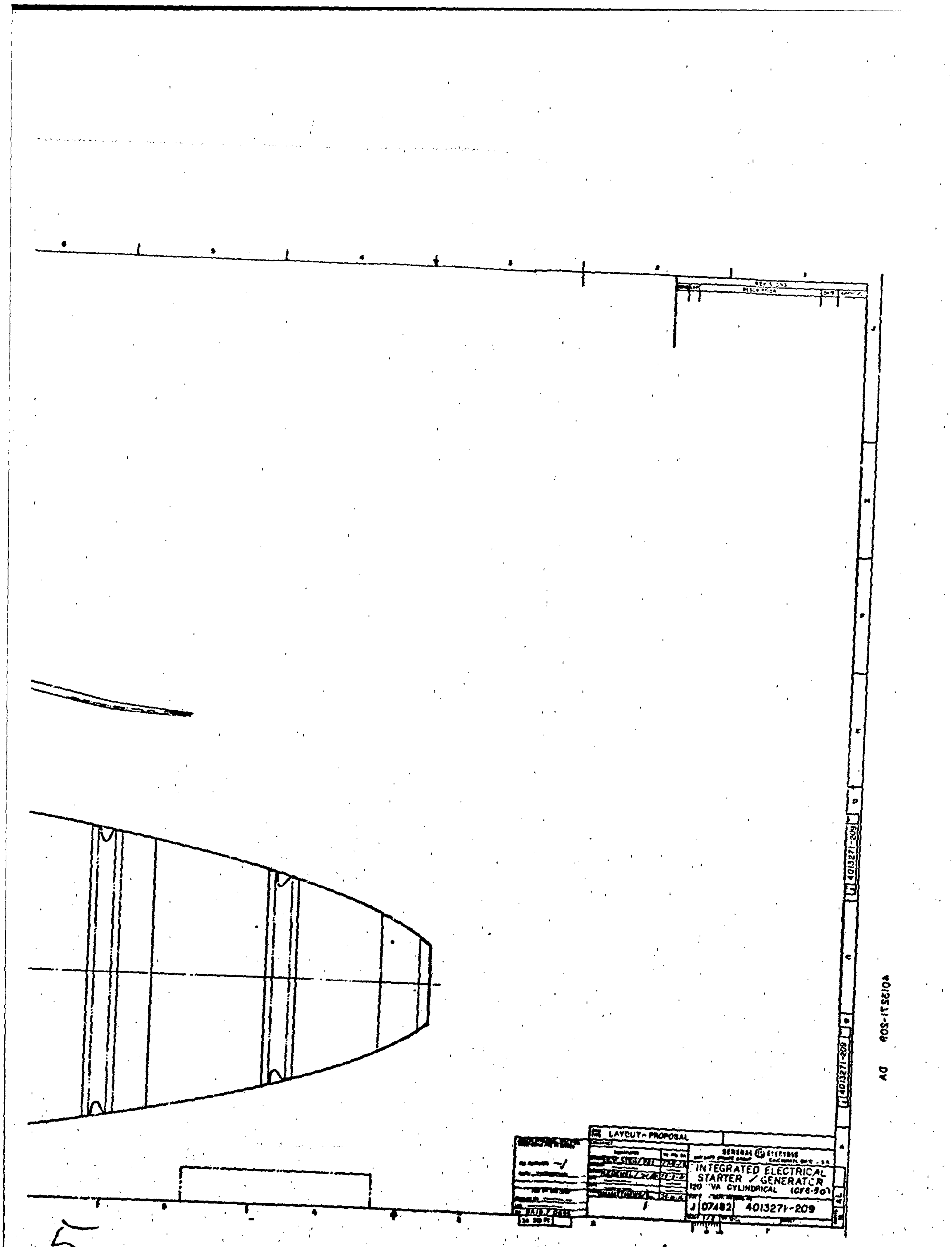

LAYOUT - PROPOSAL
GENERAL ELECTRIC
INTEGRATED ELECTRICAL
STARTER / GENERATOR
120 VA CYLINDRICAL (CF6-50)
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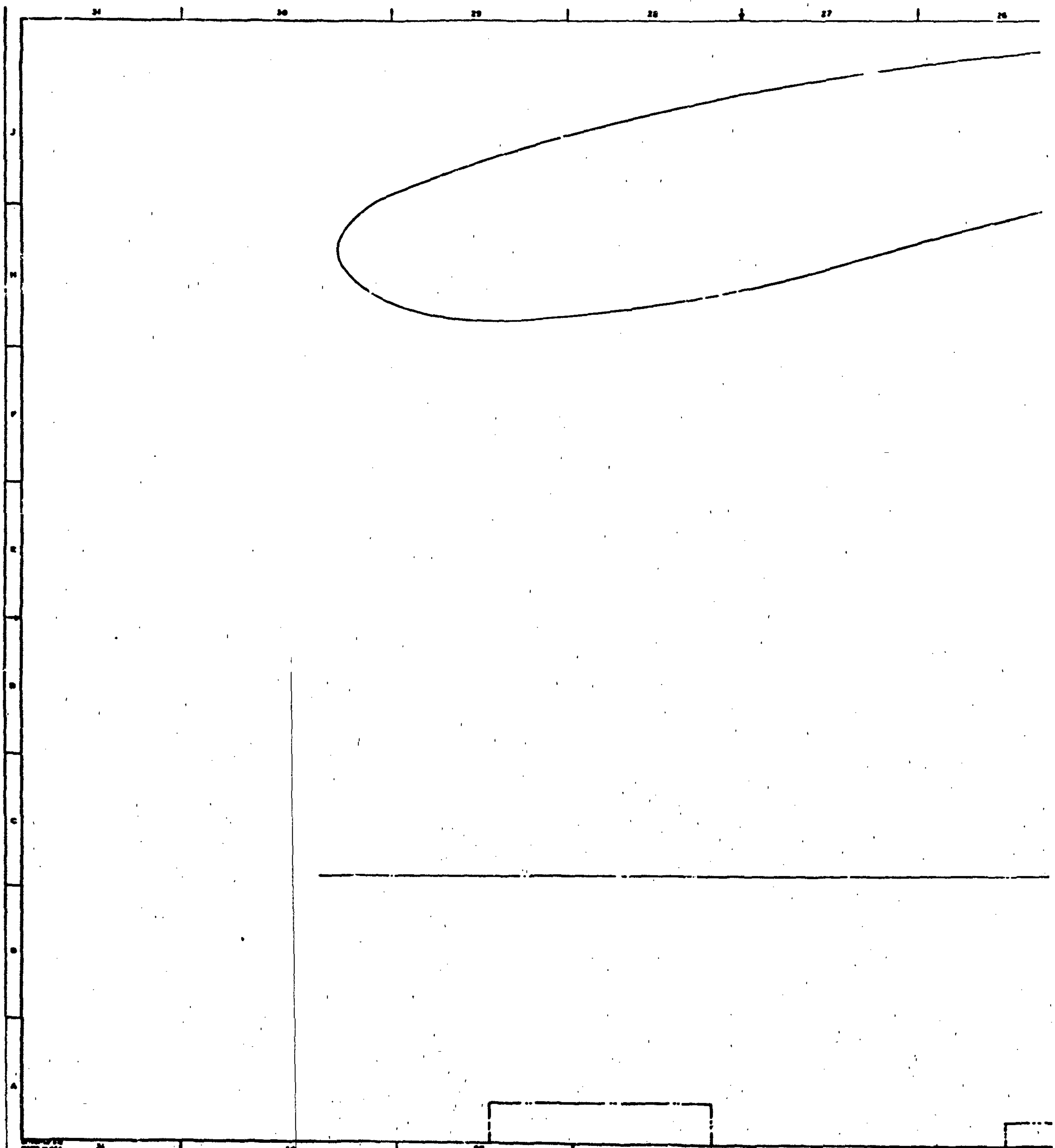
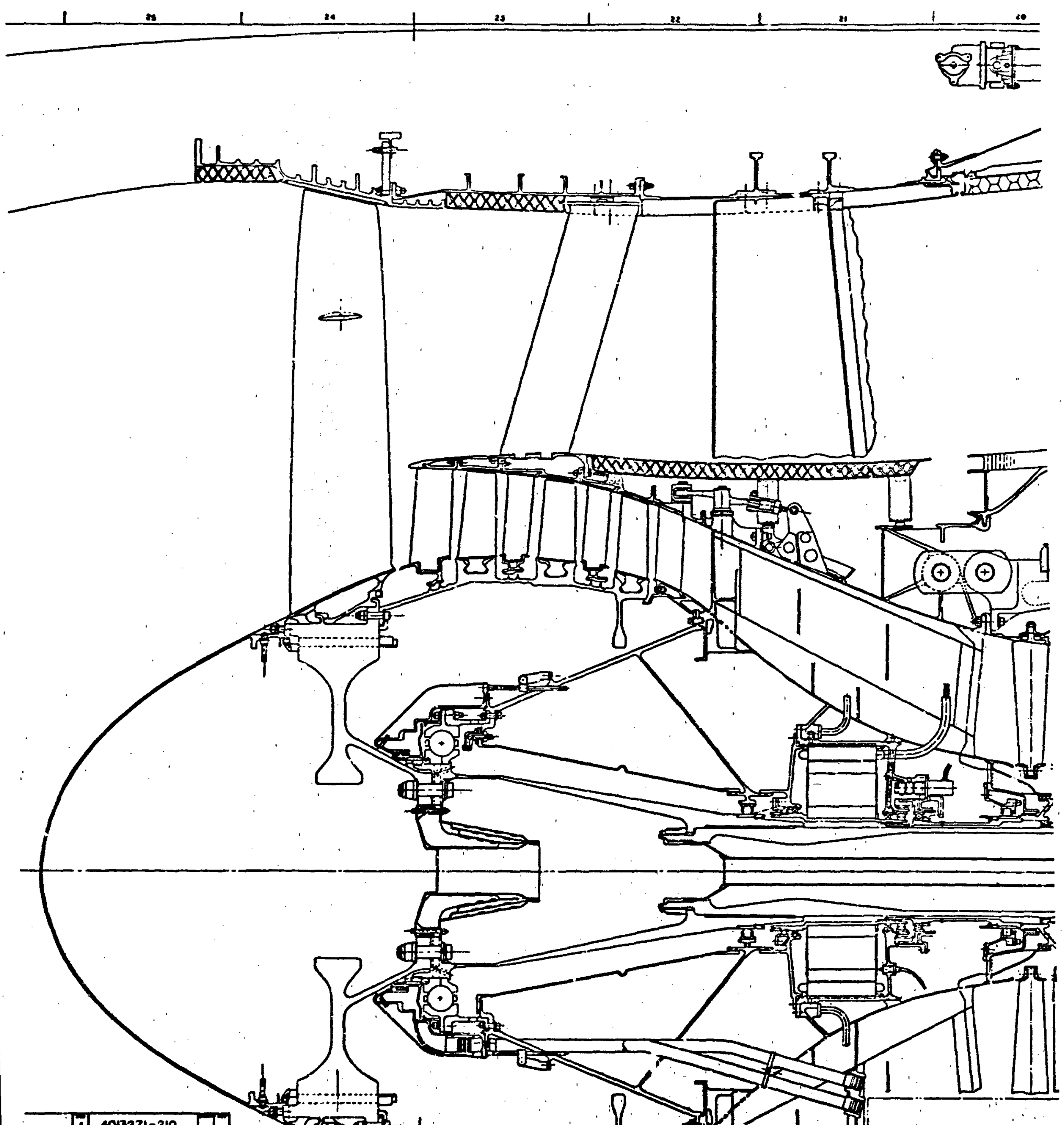
4013271-209
4013271-209
AD 4013271-209





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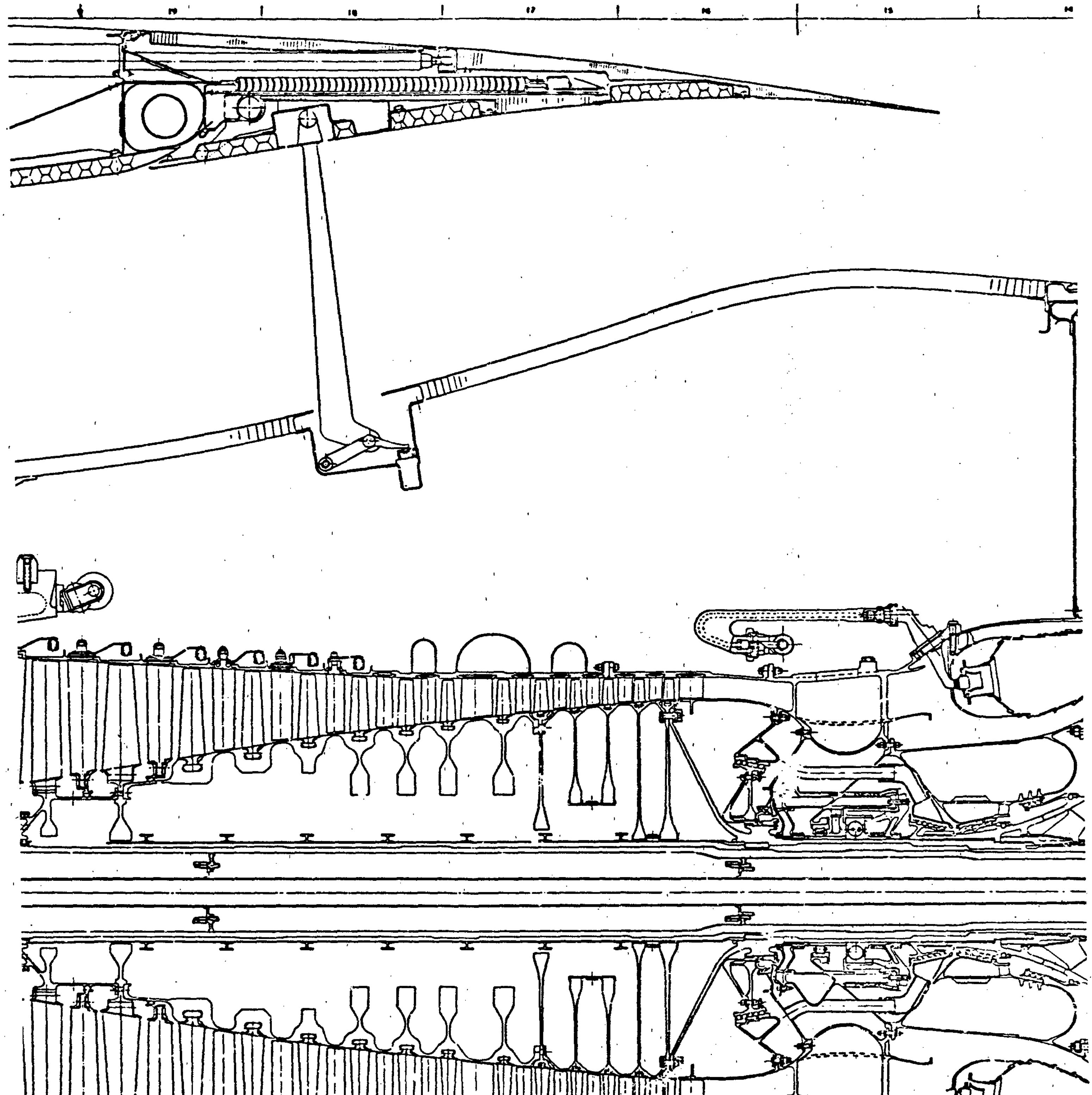
4013271-210



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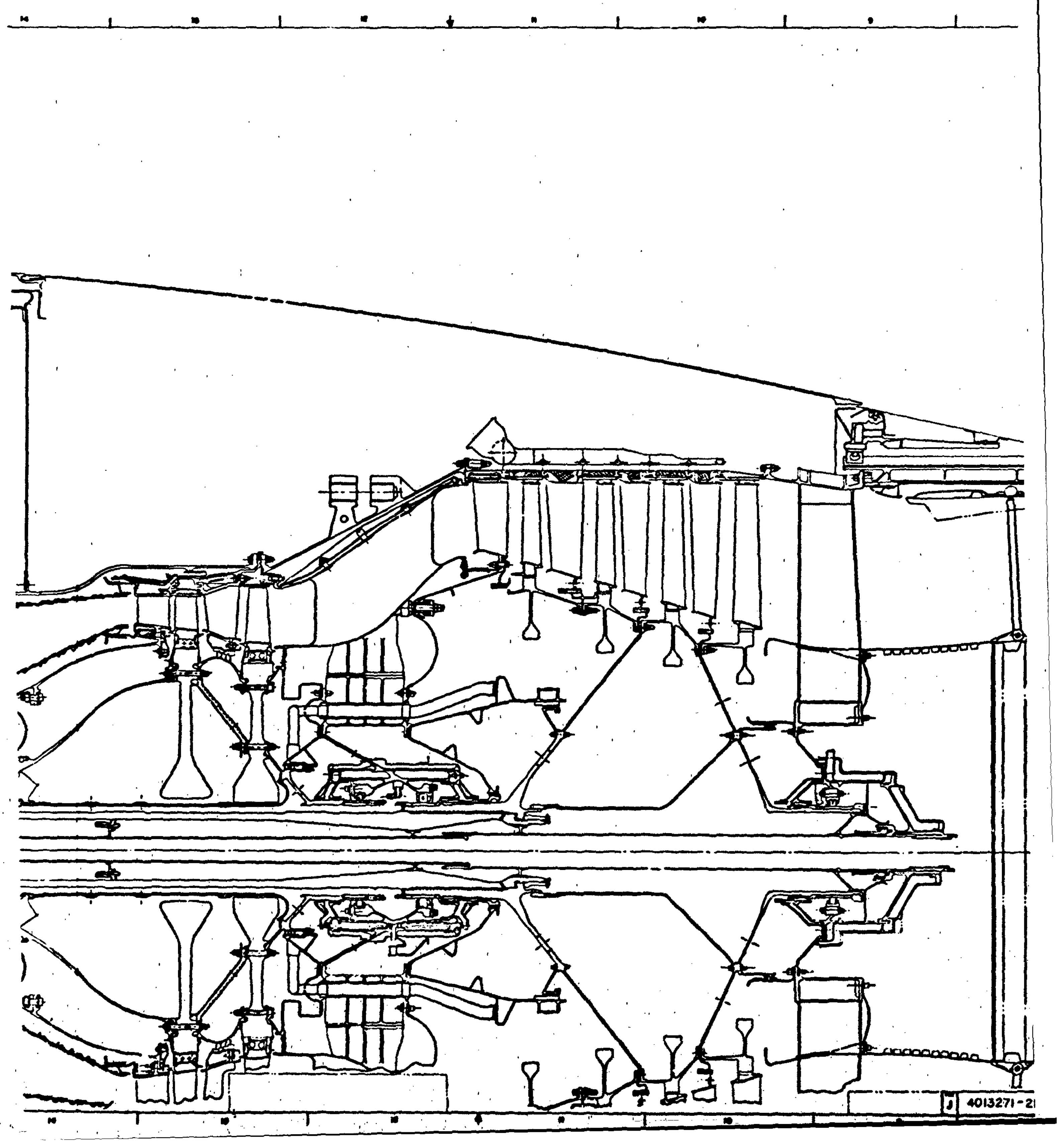
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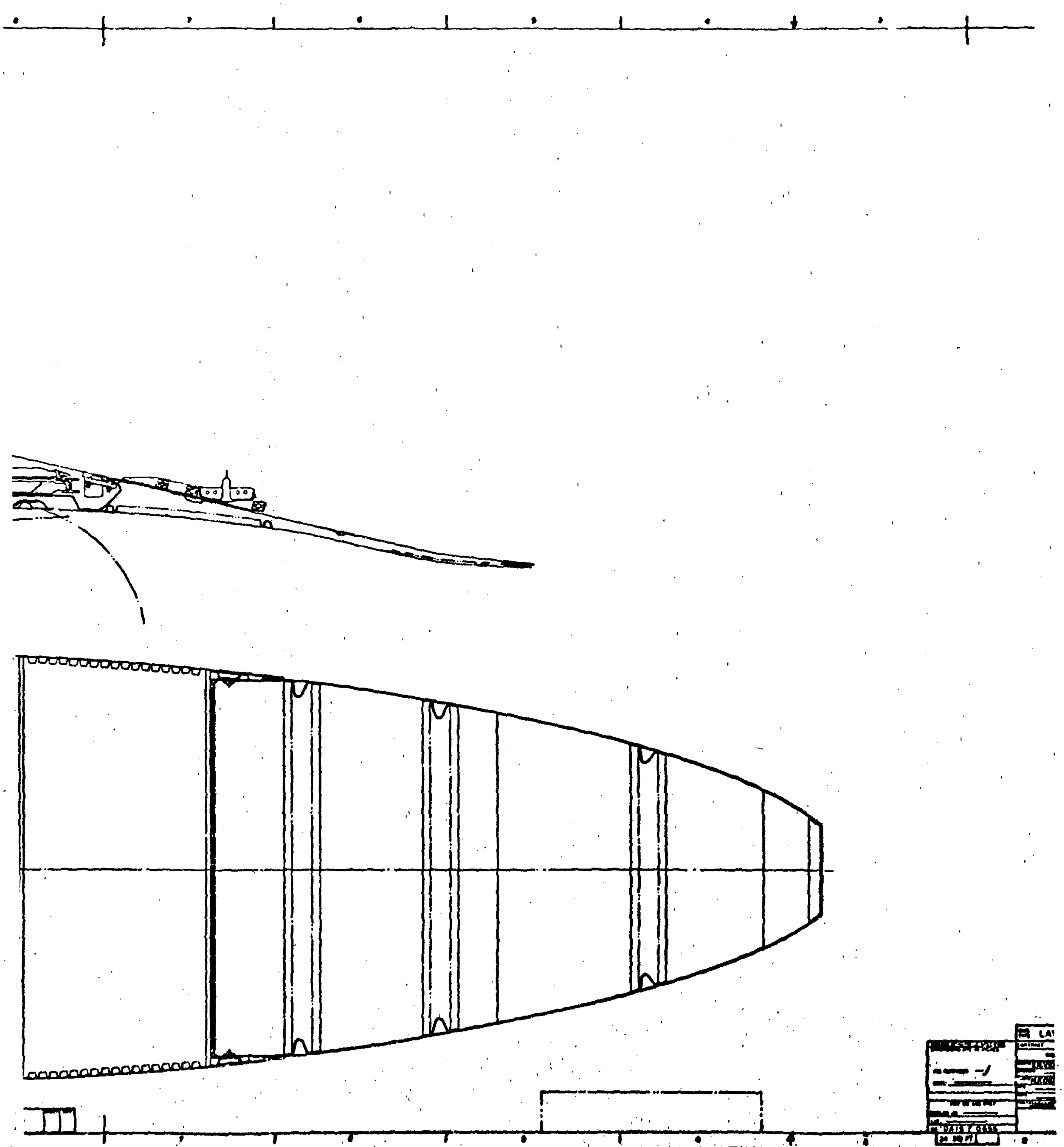

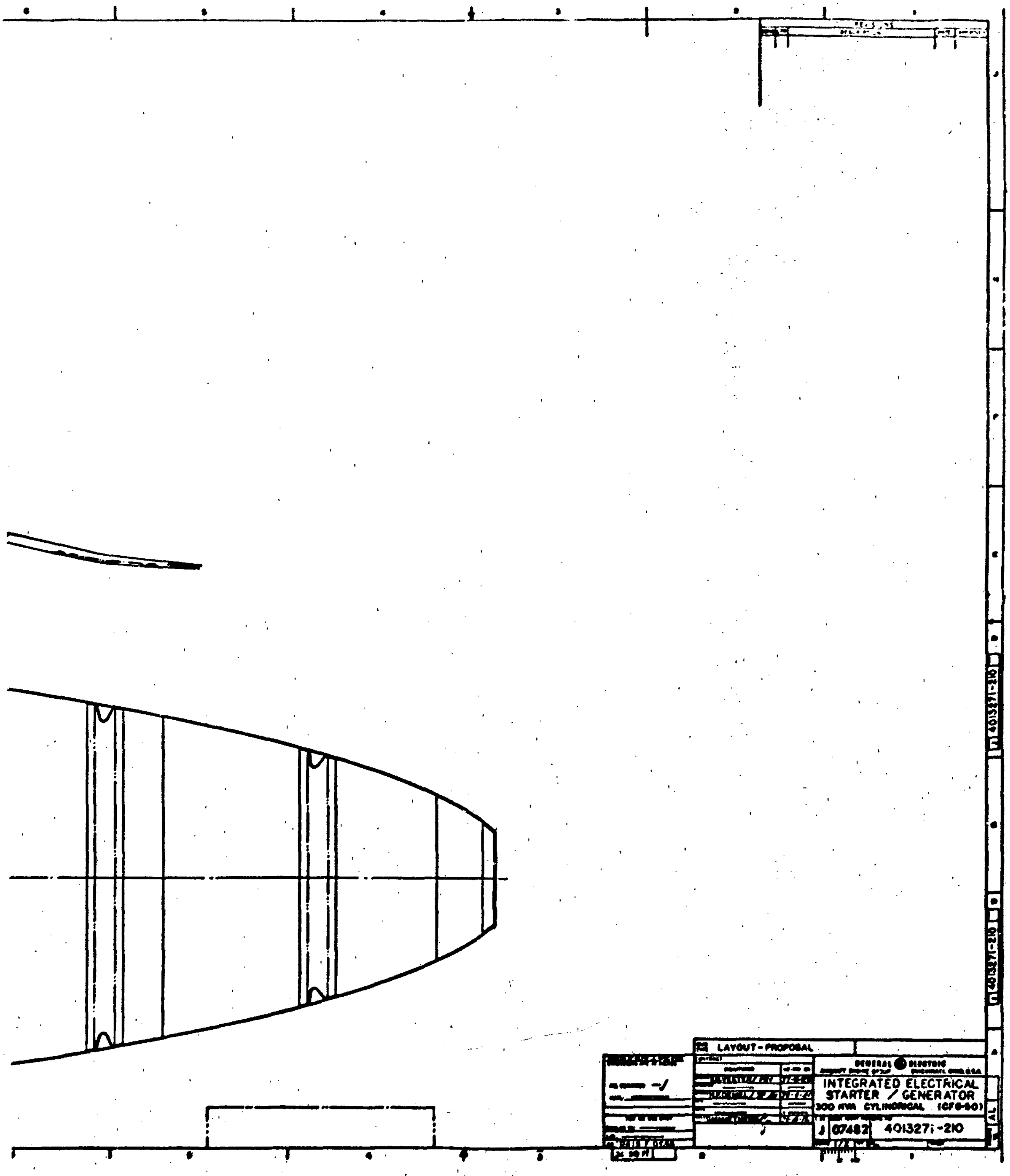

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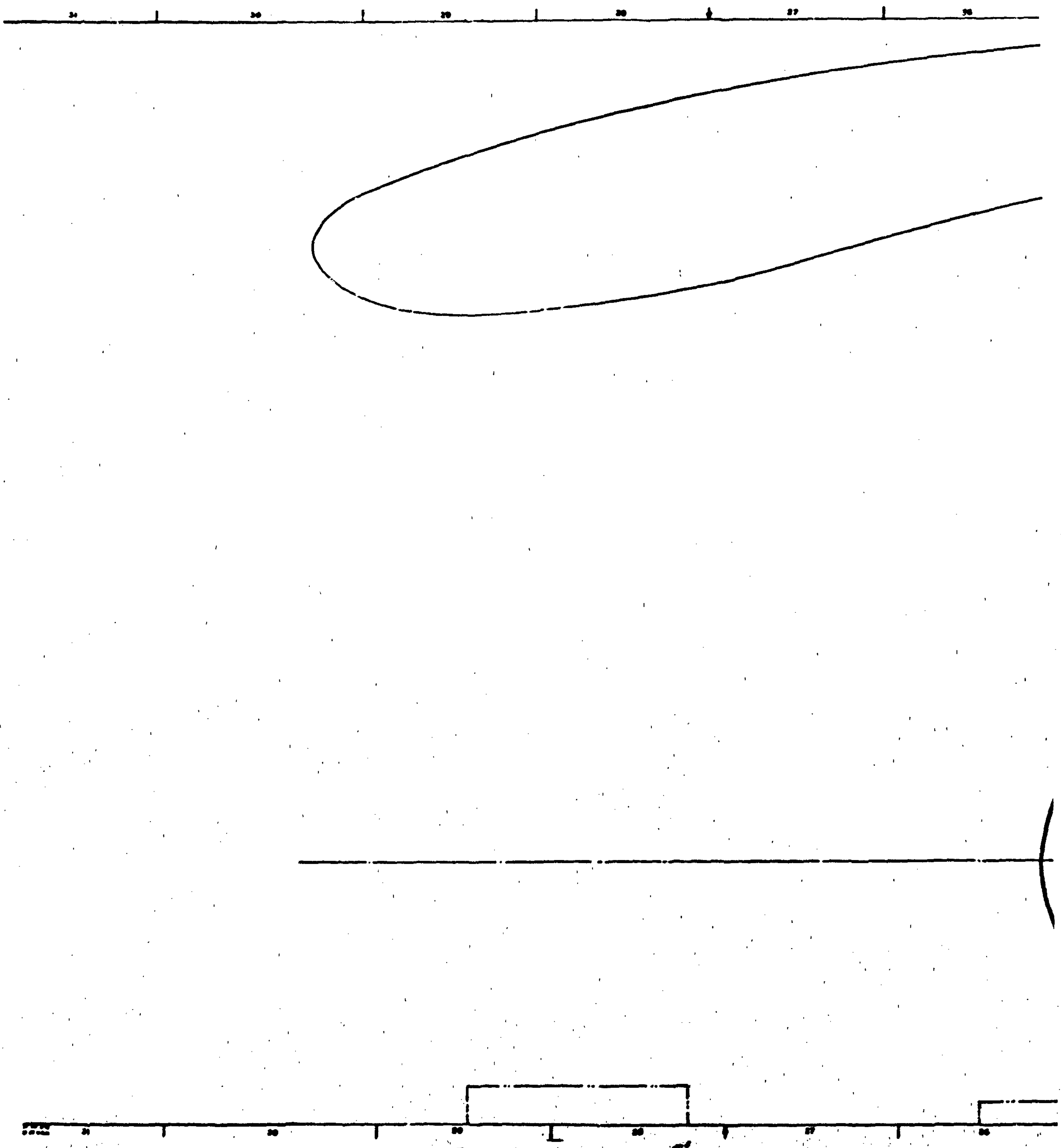
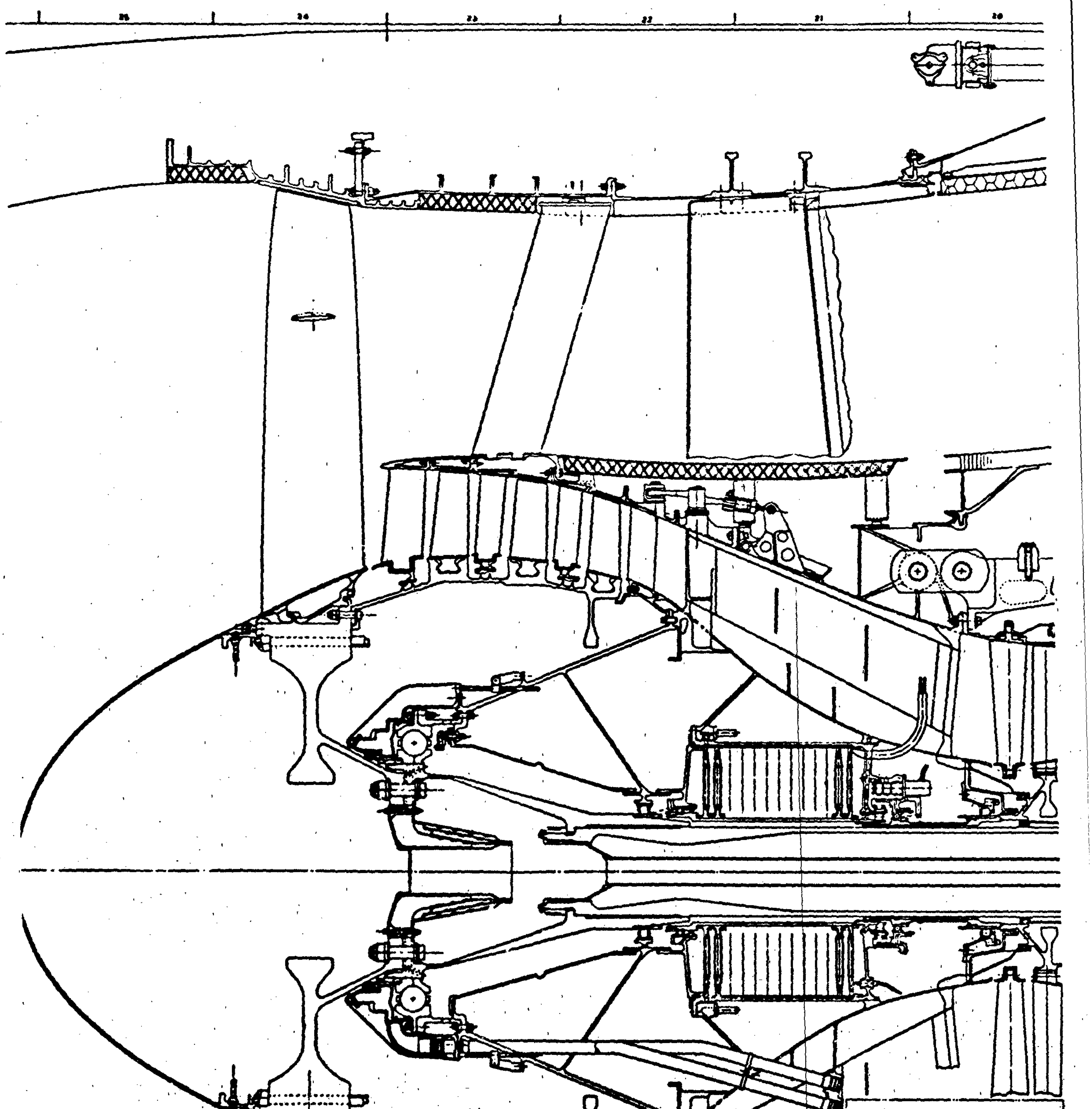
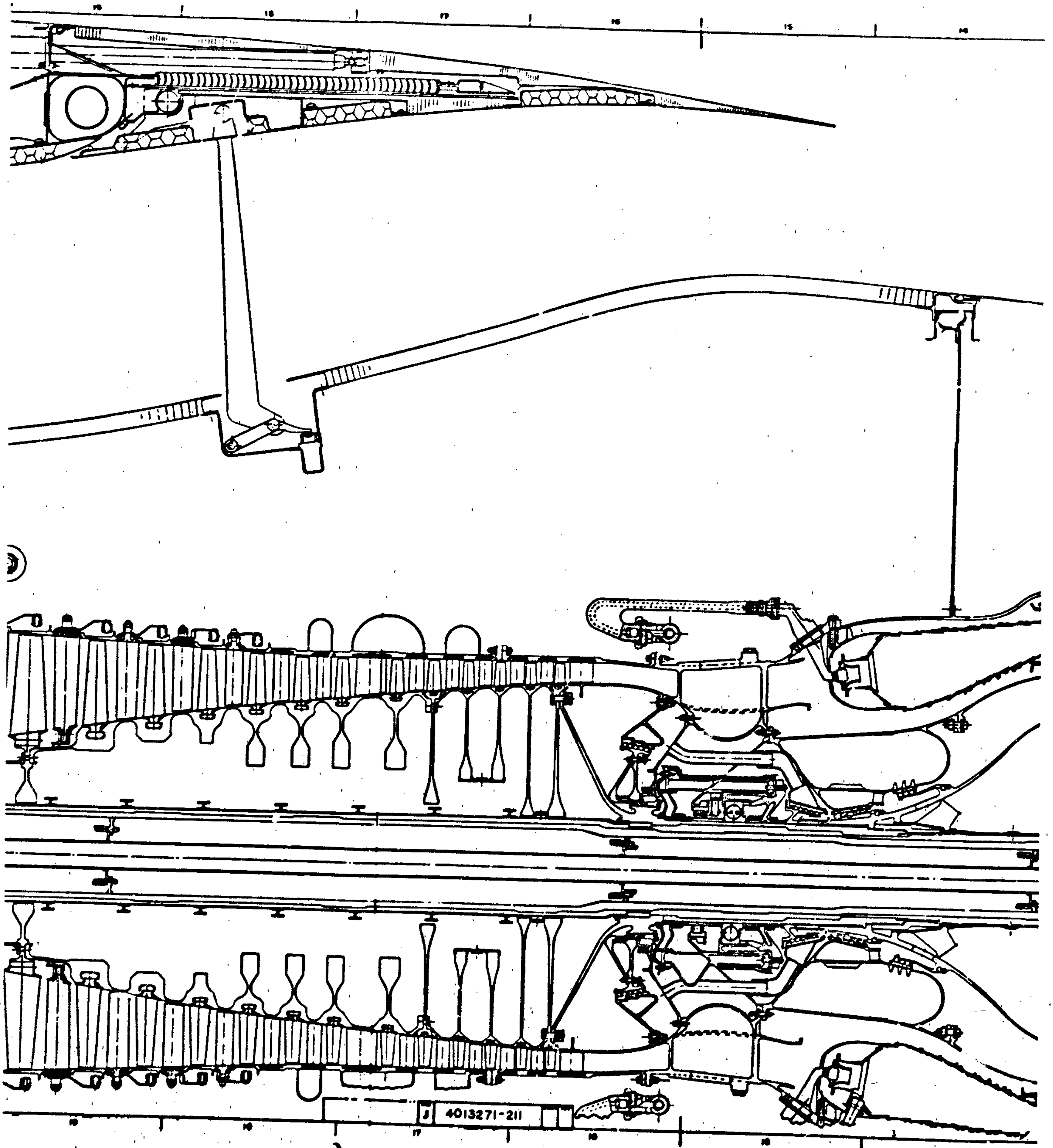




J
4013271-211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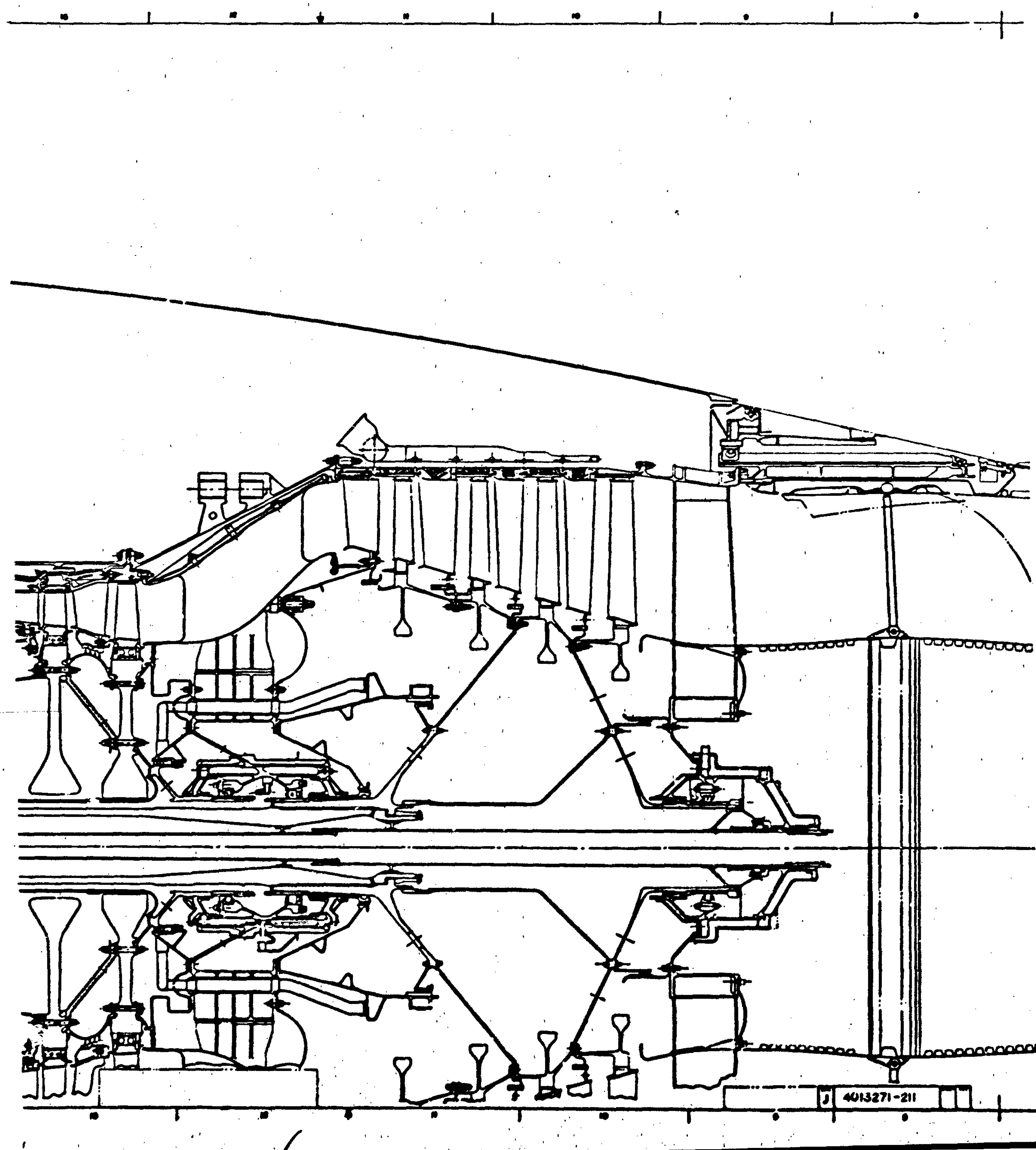
4013271-211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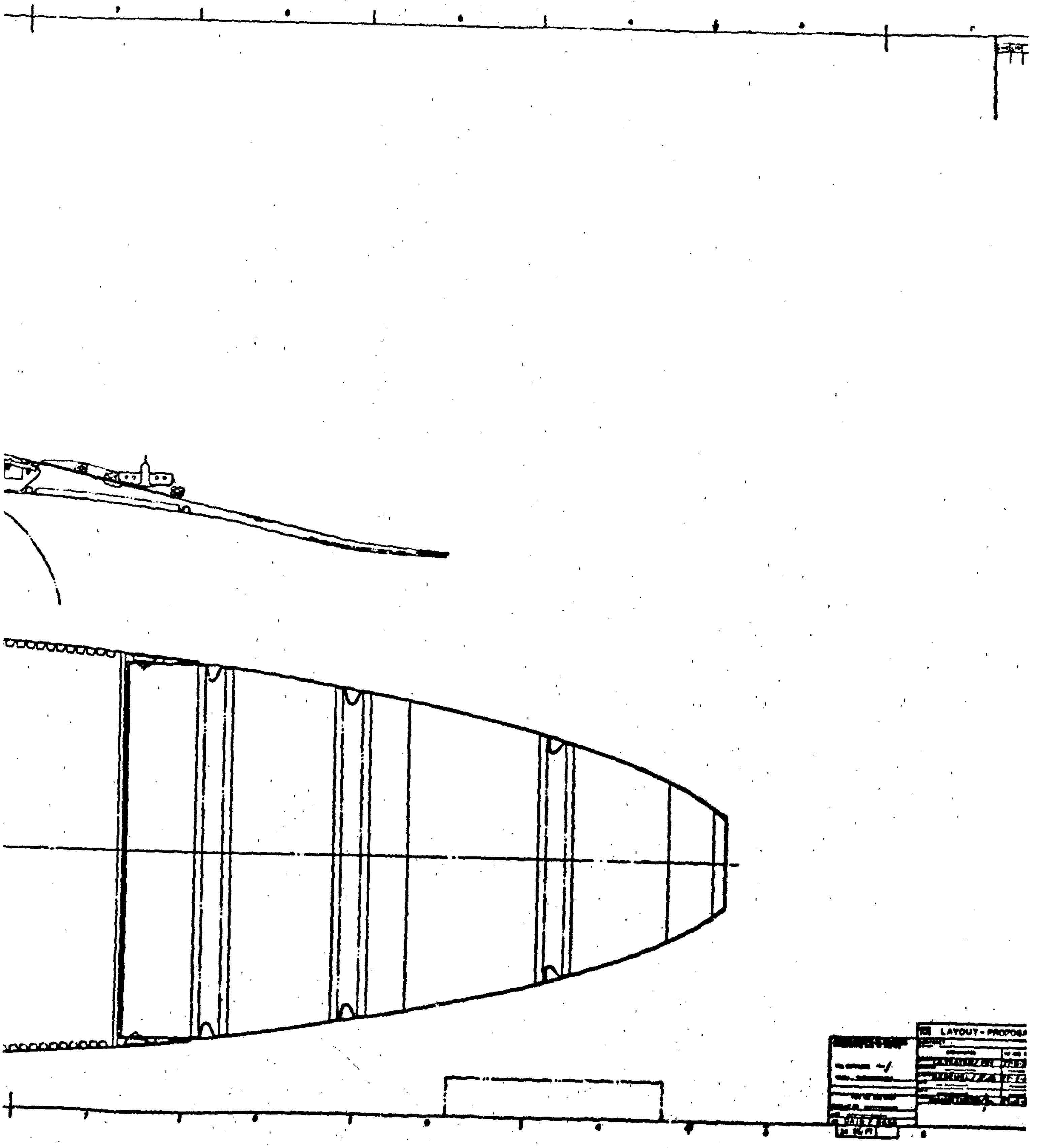
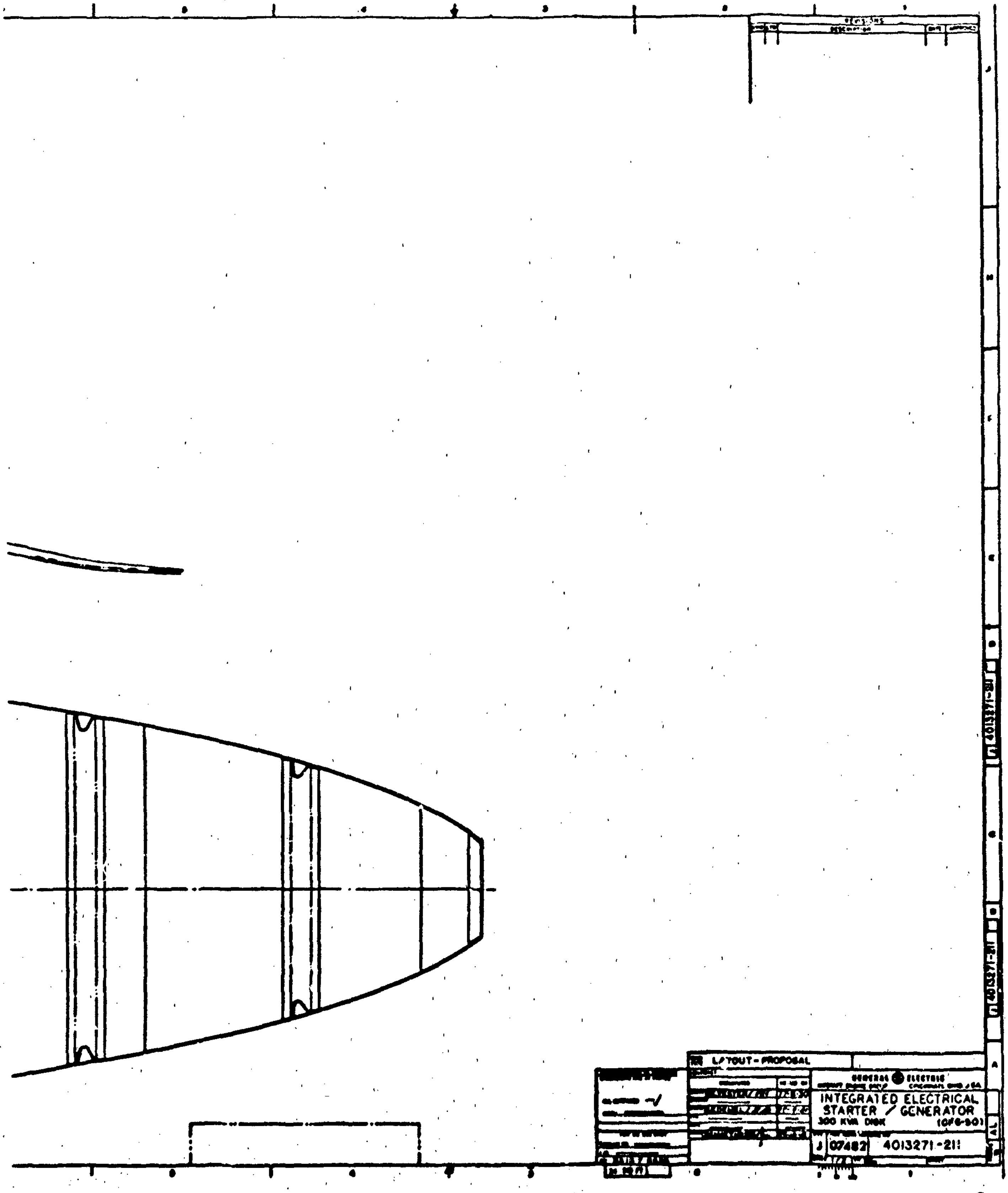
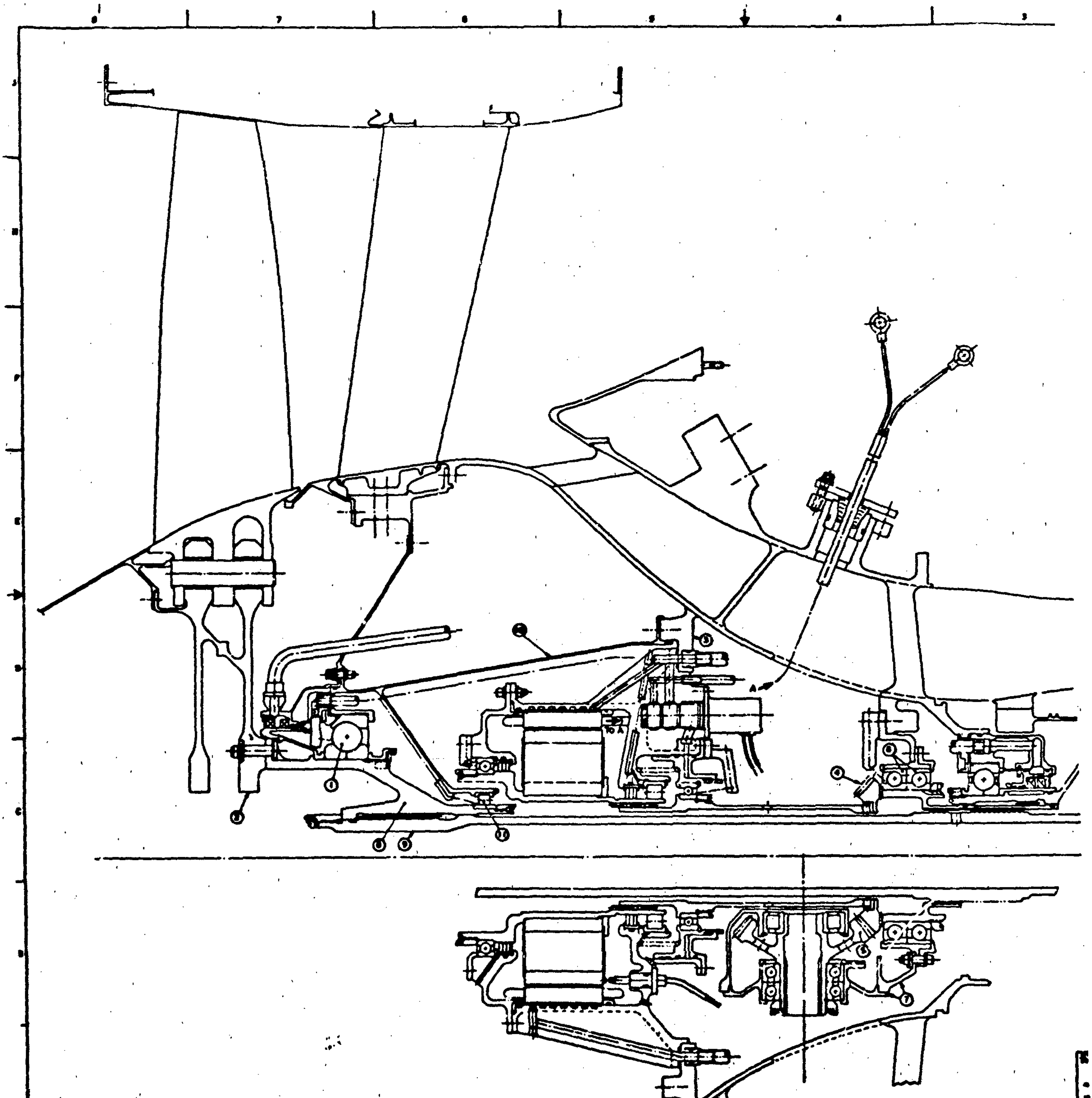
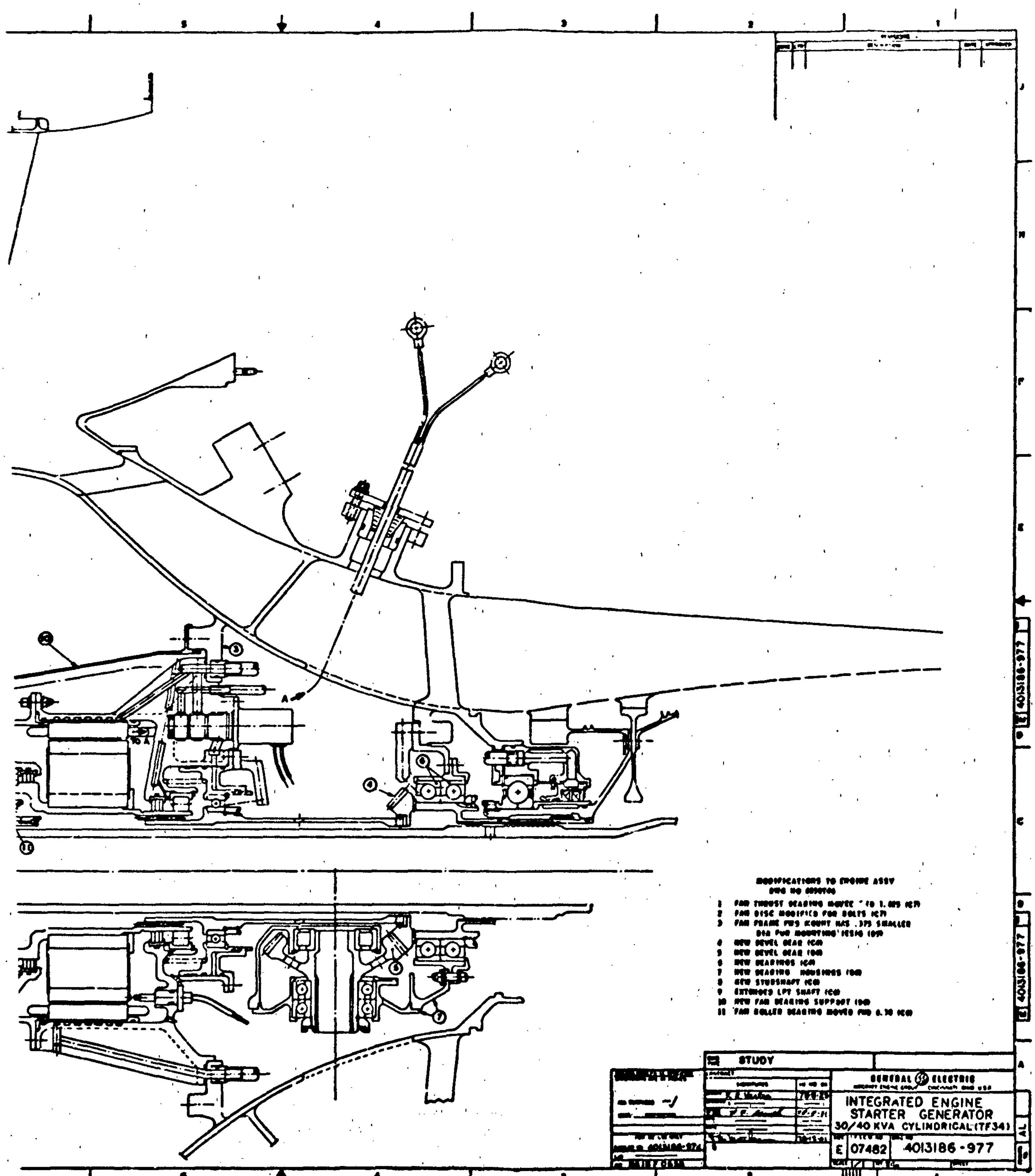
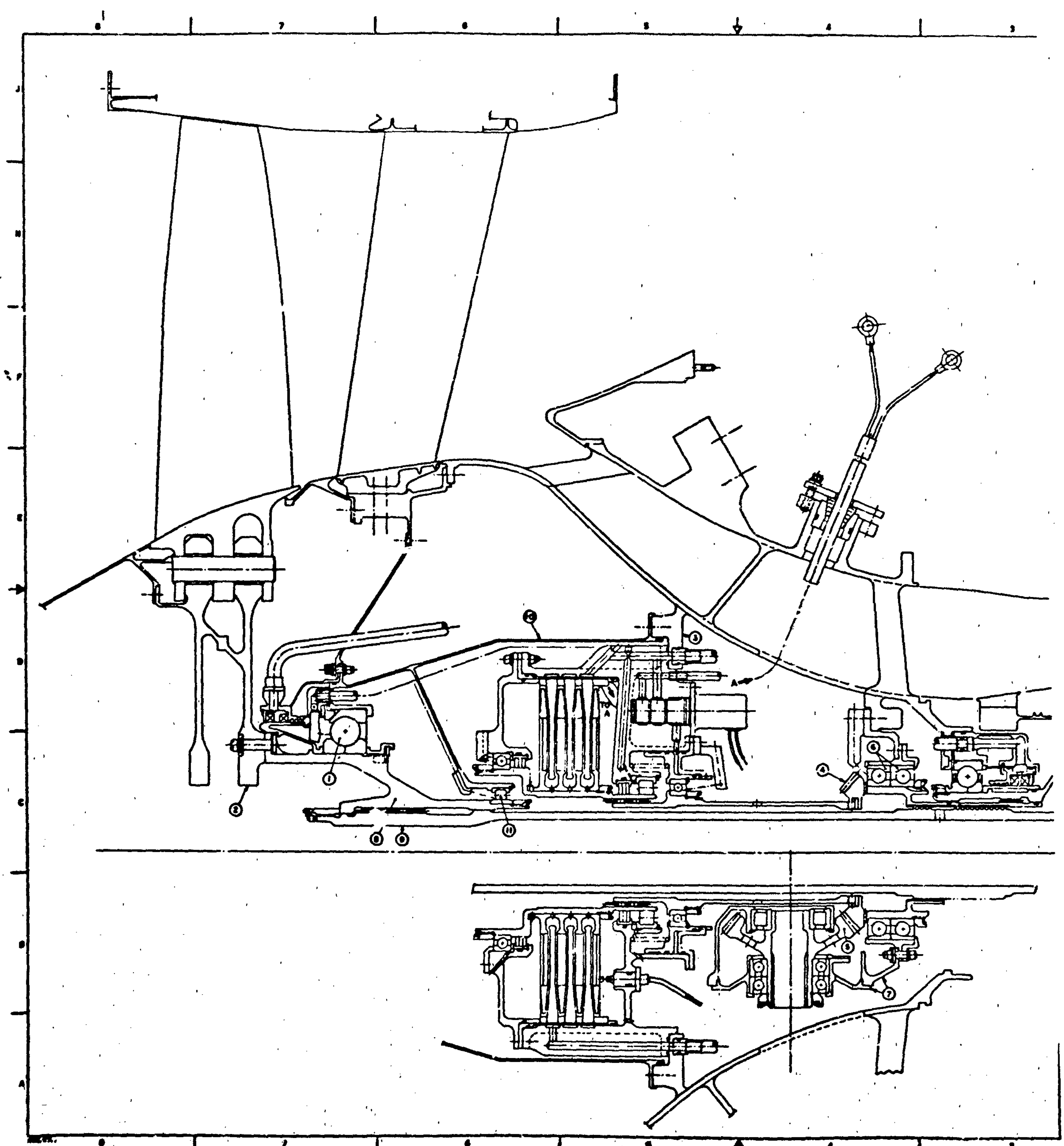
4013271-211

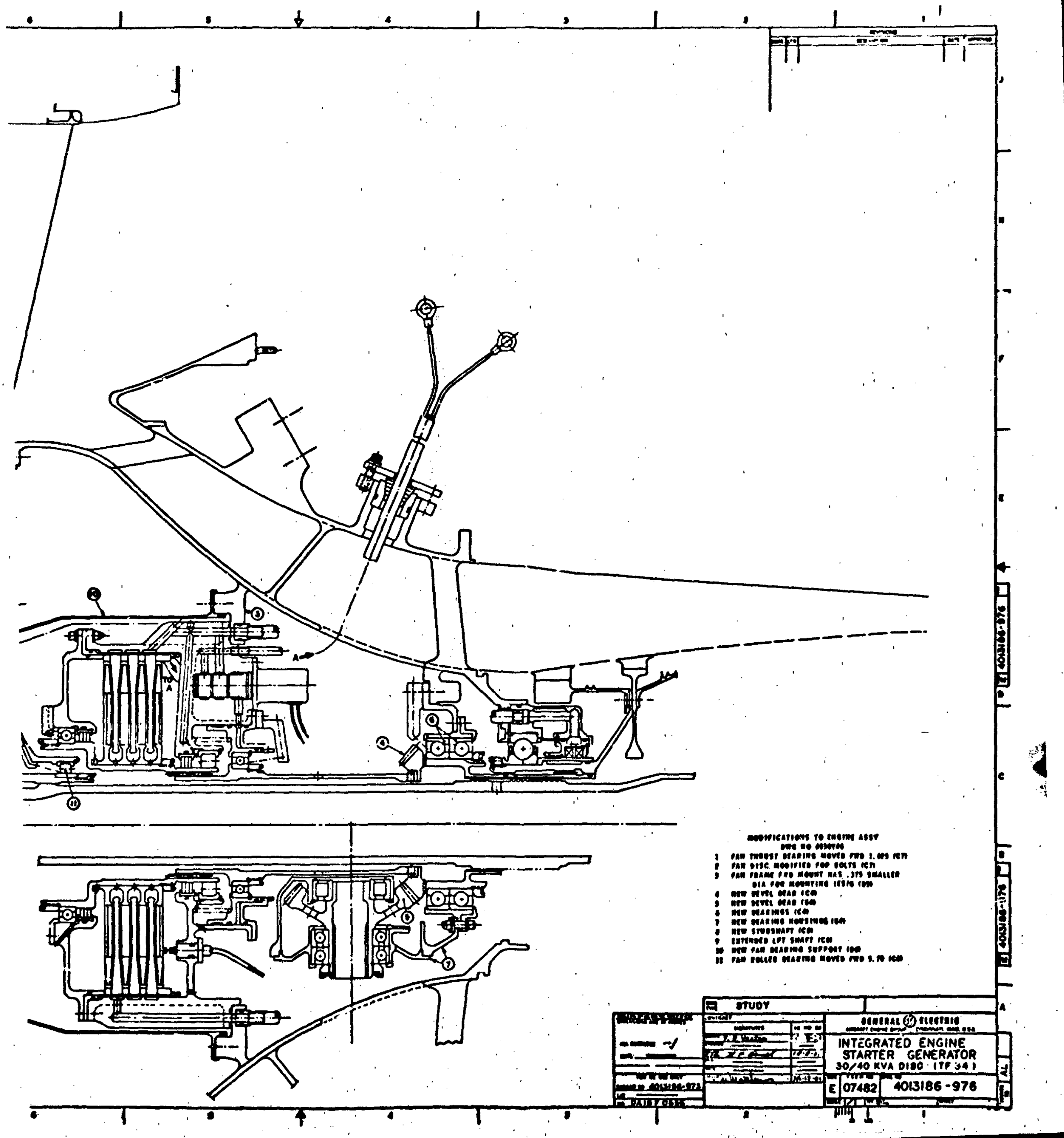

LAYOUT - PROPOSA


LAYOUT - PROPOSAL
GENERAL ELECTRIC
INTEGRATED ELECTRICAL
STARTER / GENERATOR
300 KVA DISK
(CF6-50)
J
07482
4013271-211




STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER GENERATOR
30/40 KVA CYLINDRICAL (TF34)
E 07482
4013186-977
4013186-977





MODIFICATIONS TO ENGINE ASSY
DWG NO [illegible]

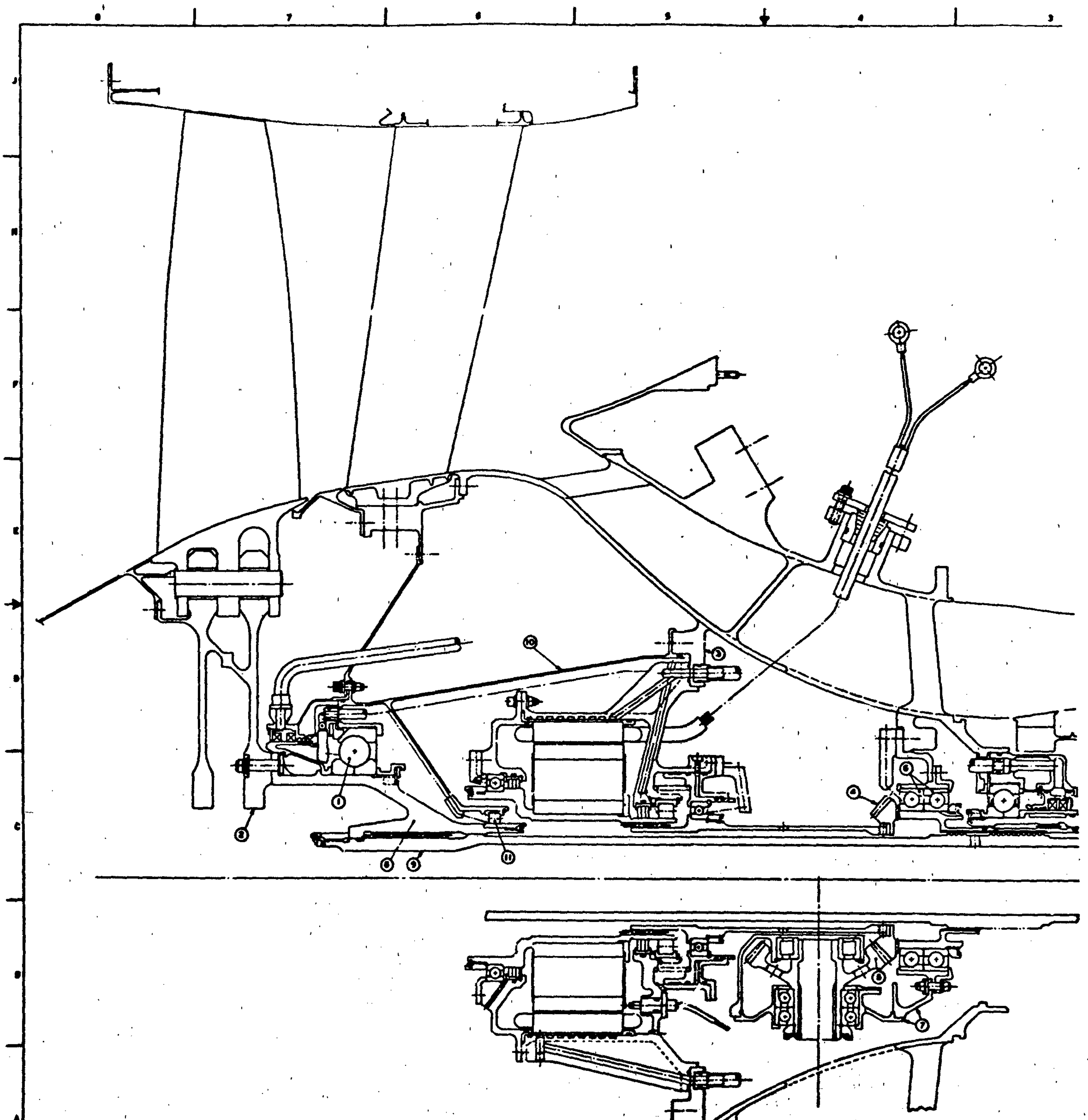
1 FAN THRUST BEARING MOVED FWD 1.[illegible] (CM)
2 FAN DISC MODIFIED FOR BOLTS (CM)
3 FAN FRAME FWD MOUNT HAS .375 SMALLER DIA FOR MOUNTING [illegible]
4 NEW BEVEL GEAR (CM)
5 NEW BEVEL GEAR (GM)
6 NEW BEARINGS (CM)
7 NEW BEARING HOUSINGS (GM)
8 NEW STUBSHAFT (CM)
9 EXTENDED LPT SHAFT (CM)
10 NEW FAN BEARING SUPPORT (GM)
11 FAN ROLLER BEARING MOVED FWD 5.70 (CM)

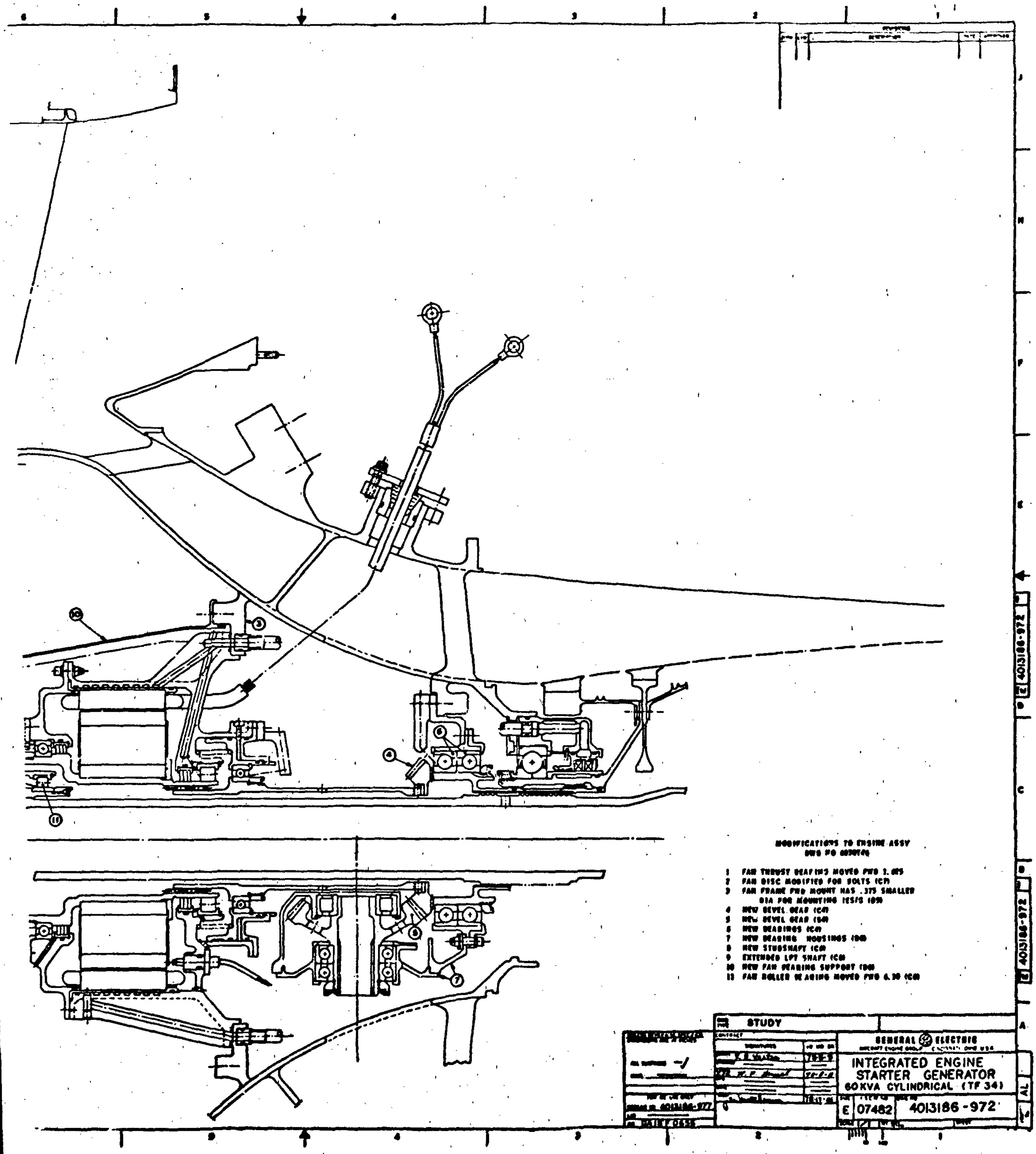
STUDY

GENERAL ELECTRIC

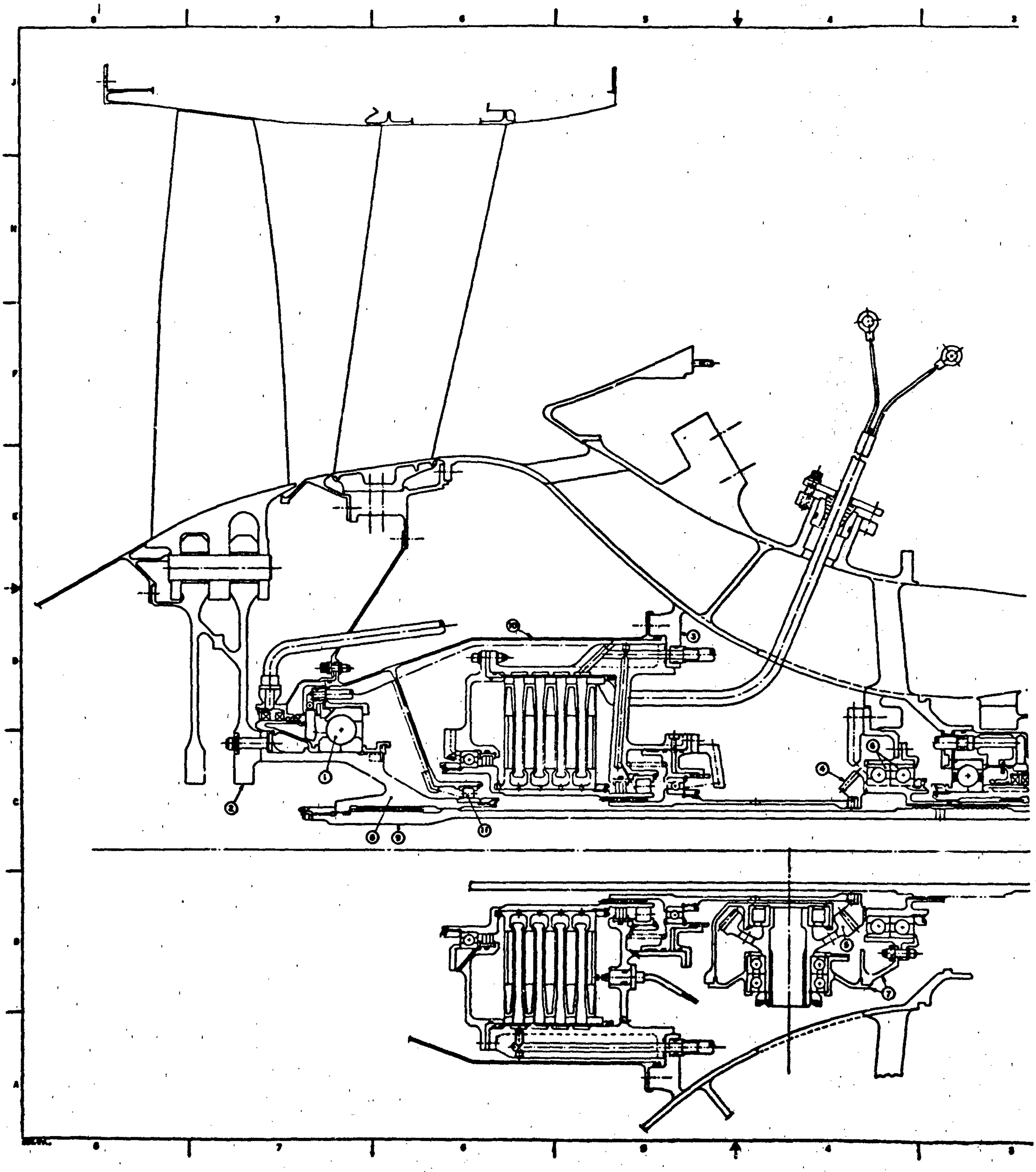
INTEGRATED ENGINE STARTER GENERATOR 30/40 KVA DISC (TF 34)

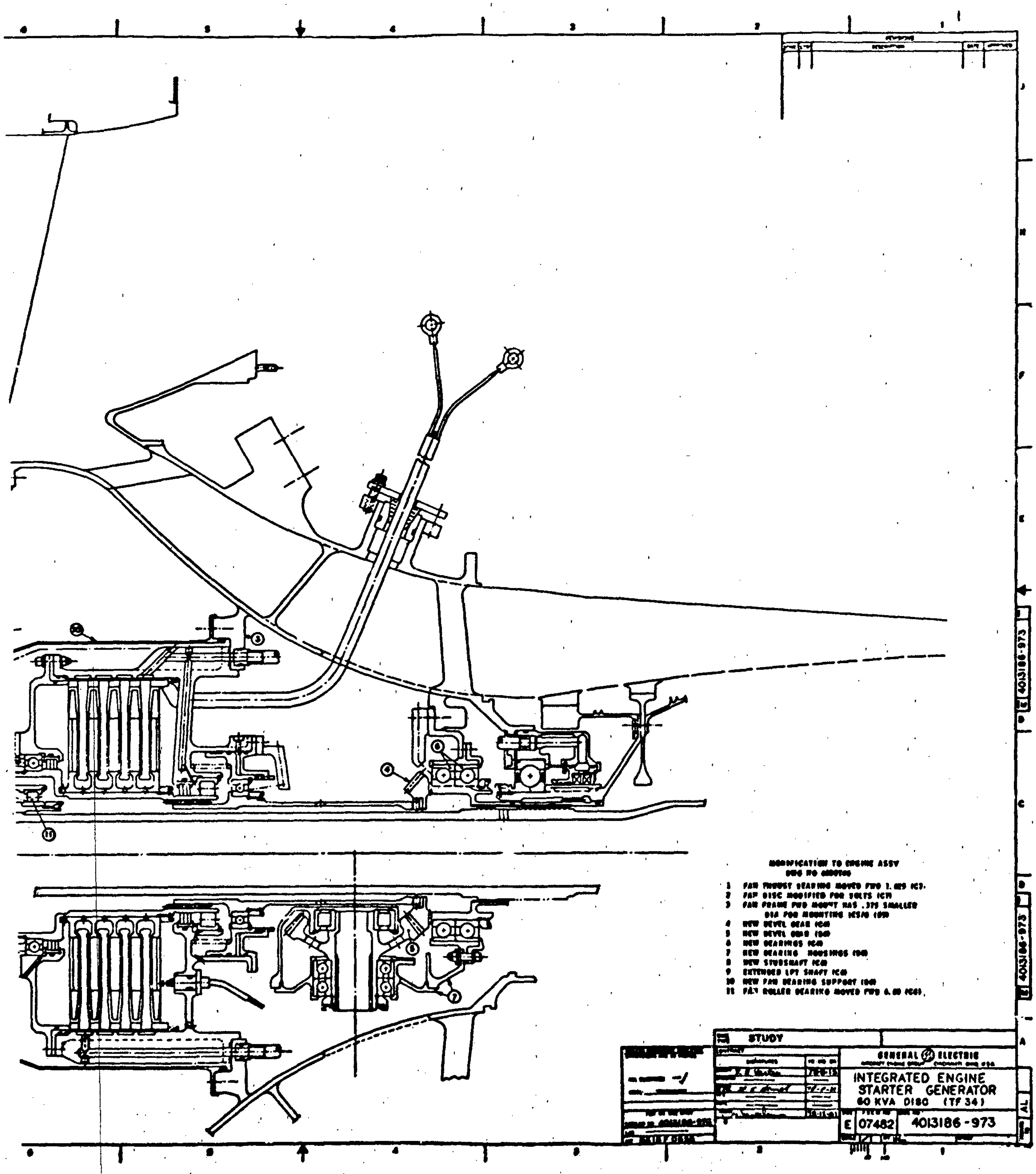
E 07482 4013186-976



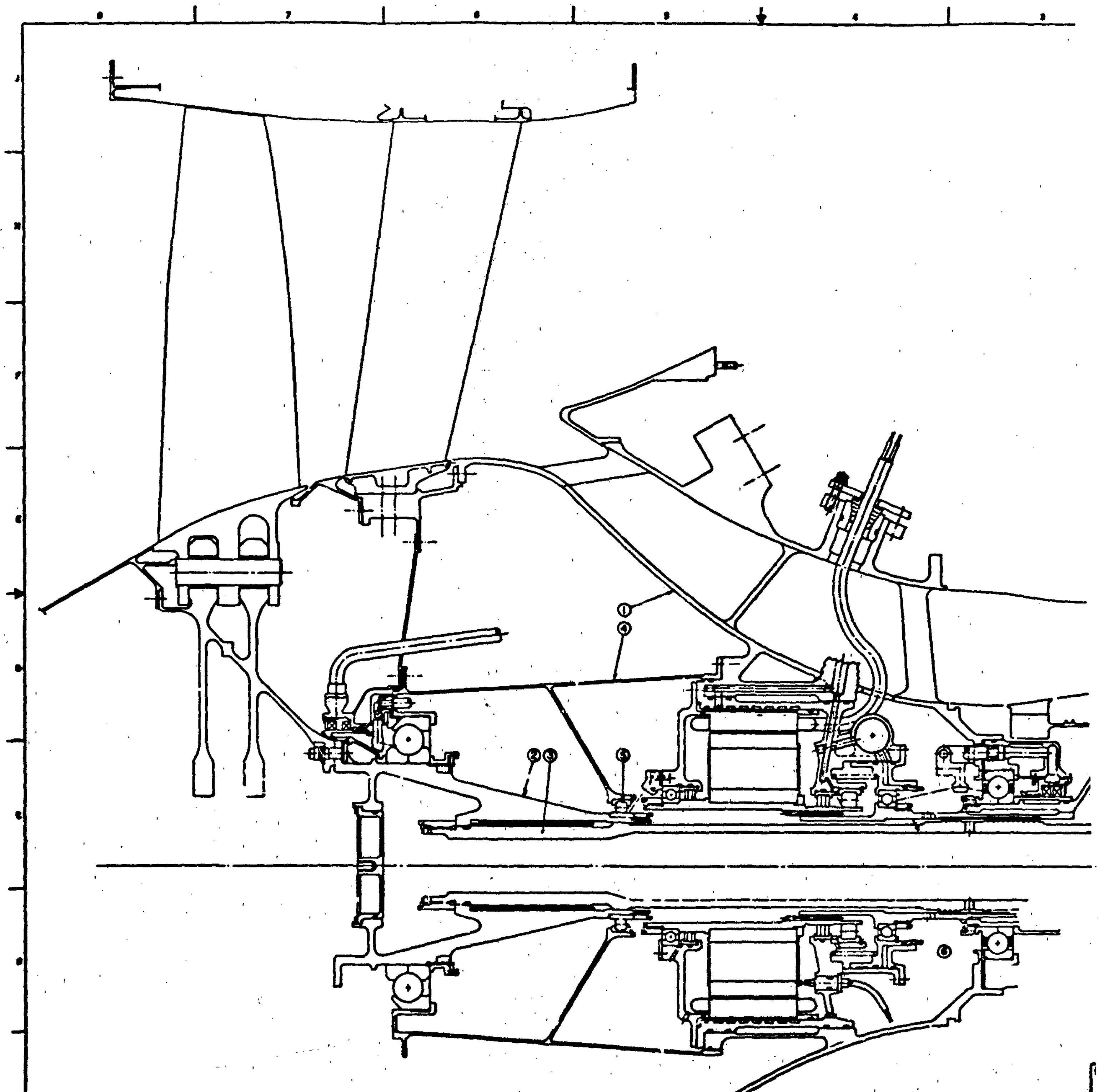


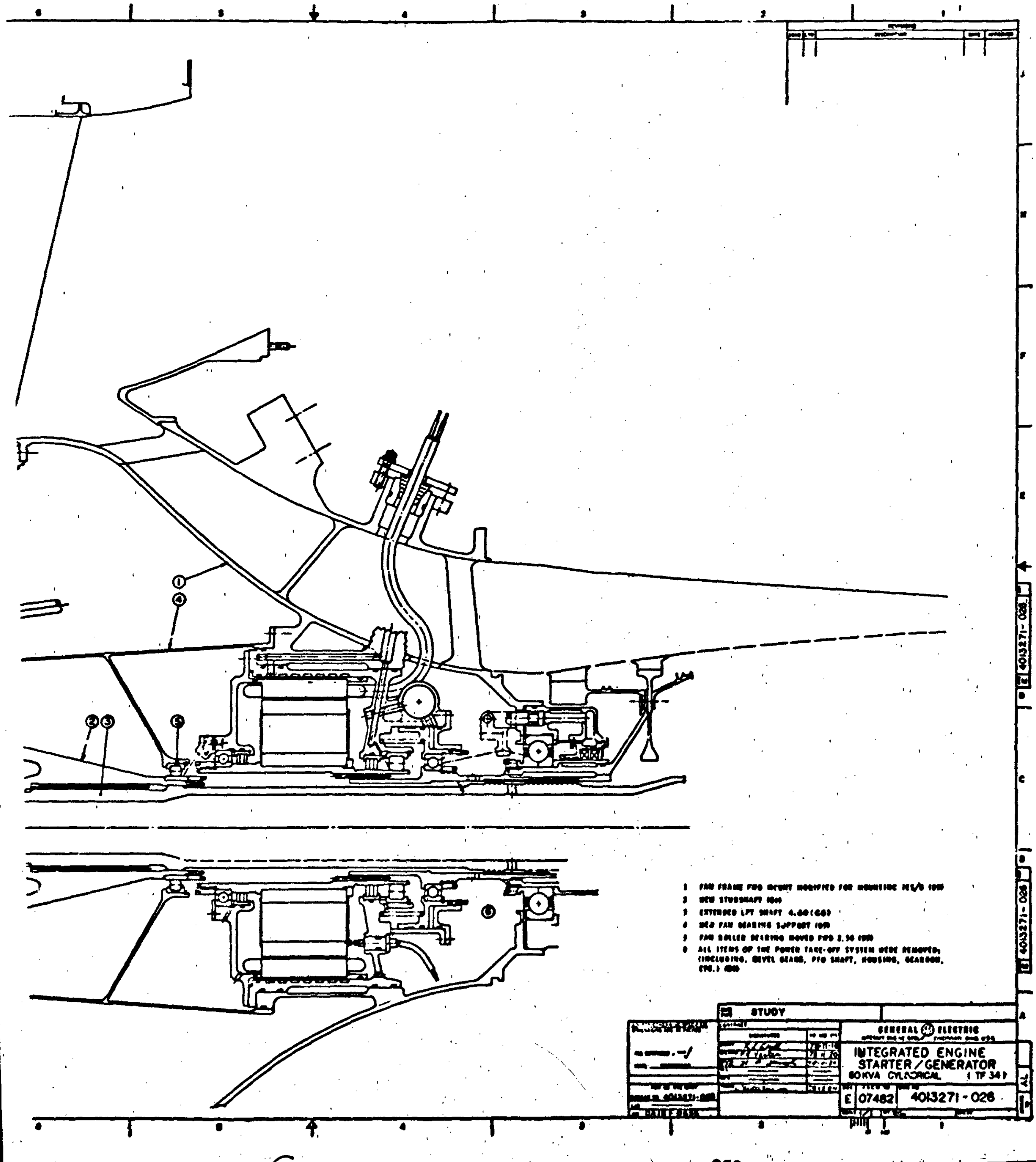
MODIFICATIONS TO ENGINE ASSY
1 FAN THRUST BEARING MOVED FWD
2 FAN DISC MODIFIED FOR BOLTS
3 FAN FRAME FWD MOUNT HAS .375 SMALLER DIA FOR MOUNTING IES/G
4 NEW BEVEL GEAR
5 NEW BEVEL GEAR
6 NEW BEARINGS
7 NEW BEARING HOUSINGS
8 NEW STUDSHAFT
9 EXTENDED LPT SHAFT
10 NEW FAN BEARING SUPPORT
11 FAN ROLLER BEARING MOVED FWD
STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE STARTER GENERATOR
60 KVA CYLINDRICAL (TF 34)
E
07482
4013186-972
4013186-972
AL

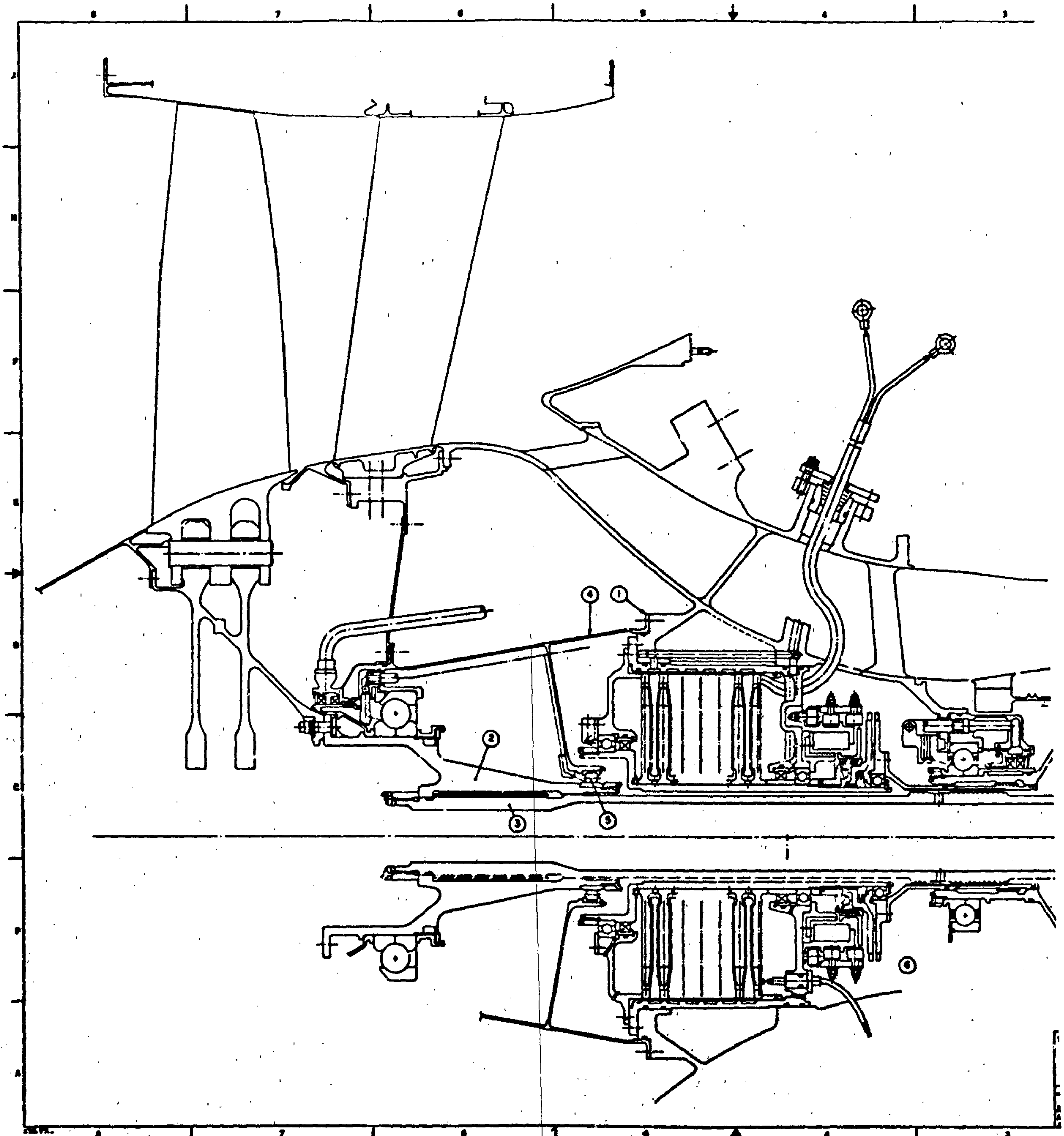




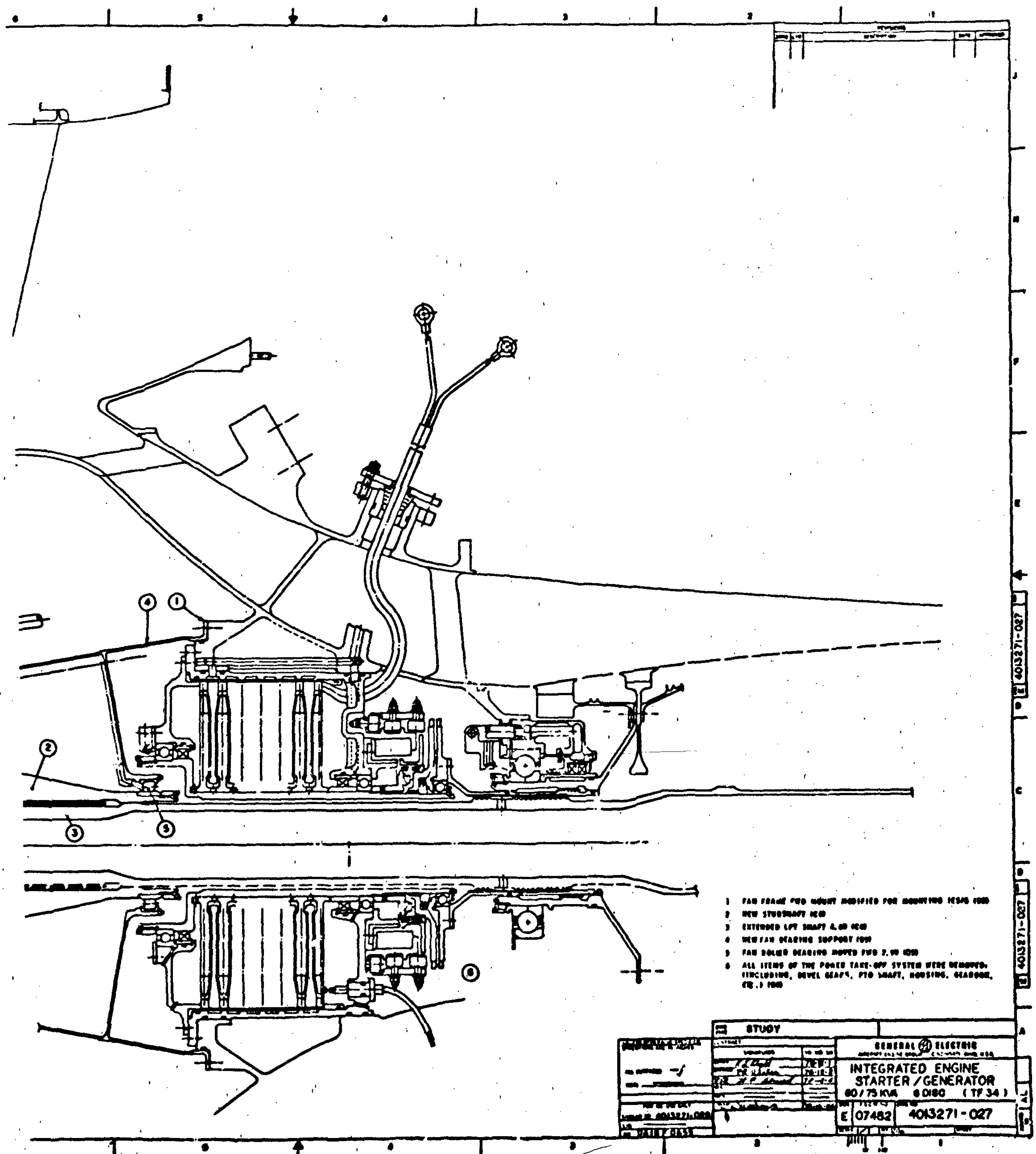
MODIFICATION TO ENGINE ASSY
1 FAN THRUST BEARING MOVED FWD
2 FAN DISC MODIFIED FOR BOLTS
3 FAN FRAME FWD MOUNT HAS .375 SMALLER DIA FOR MOUNTING
4 NEW BEVEL GEAR
5 NEW BEVEL GEAR
6 NEW BEARINGS
7 NEW BEARING HOUSINGS
8 NEW STUBSHAFT
9 EXTENDED LPT SHAFT
10 NEW FAN BEARING SUPPORT
11 FAN ROLLER BEARING MOVED FWD
STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER GENERATOR
60 KVA DISC (TF 34)
E
07482
4013186-973
AL



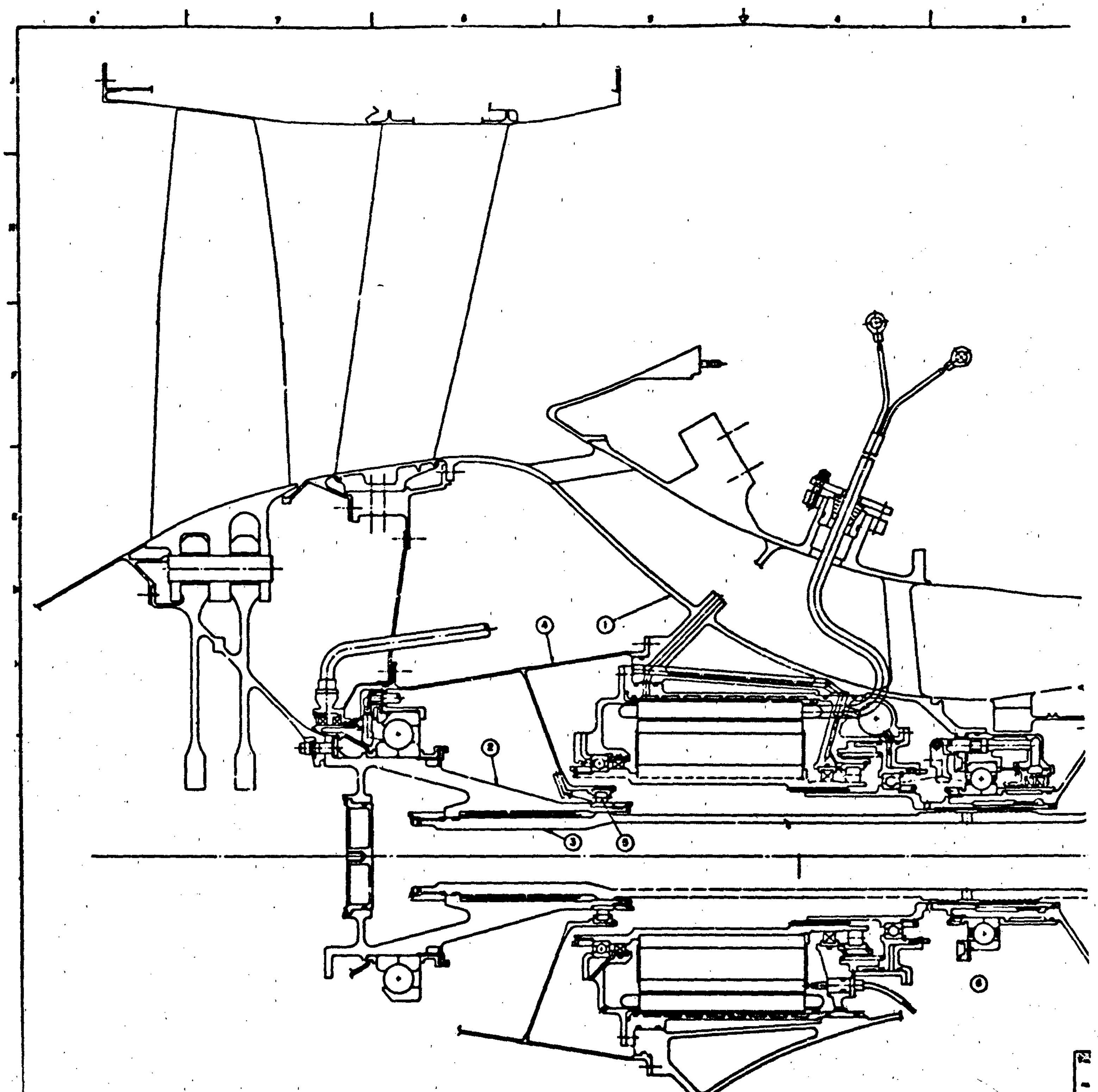

1 FAN FRAME FWD MOUNT MODIFIED FOR MOUNTING IES/G (GE)
2 NEW STUBSHAFT (GE)
3 EXTENDED LPT SHAFT 4.00 (GE)
4 NEW FAN BEARING SUPPORT (GE)
5 FAN ROLLER BEARING MOVED FWD 2.50 (GE)
6 ALL ITEMS OF THE POWER TAKE-OFF SYSTEM WERE REMOVED; (INCLUDING, BEVEL GEARS, PTO SHAFT, HOUSING, GEARBOX, ETC.) (GE)
STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER / GENERATOR
60 KVA CYLINDRICAL (TF 34)
E
07482
4013271-026
4013271-026



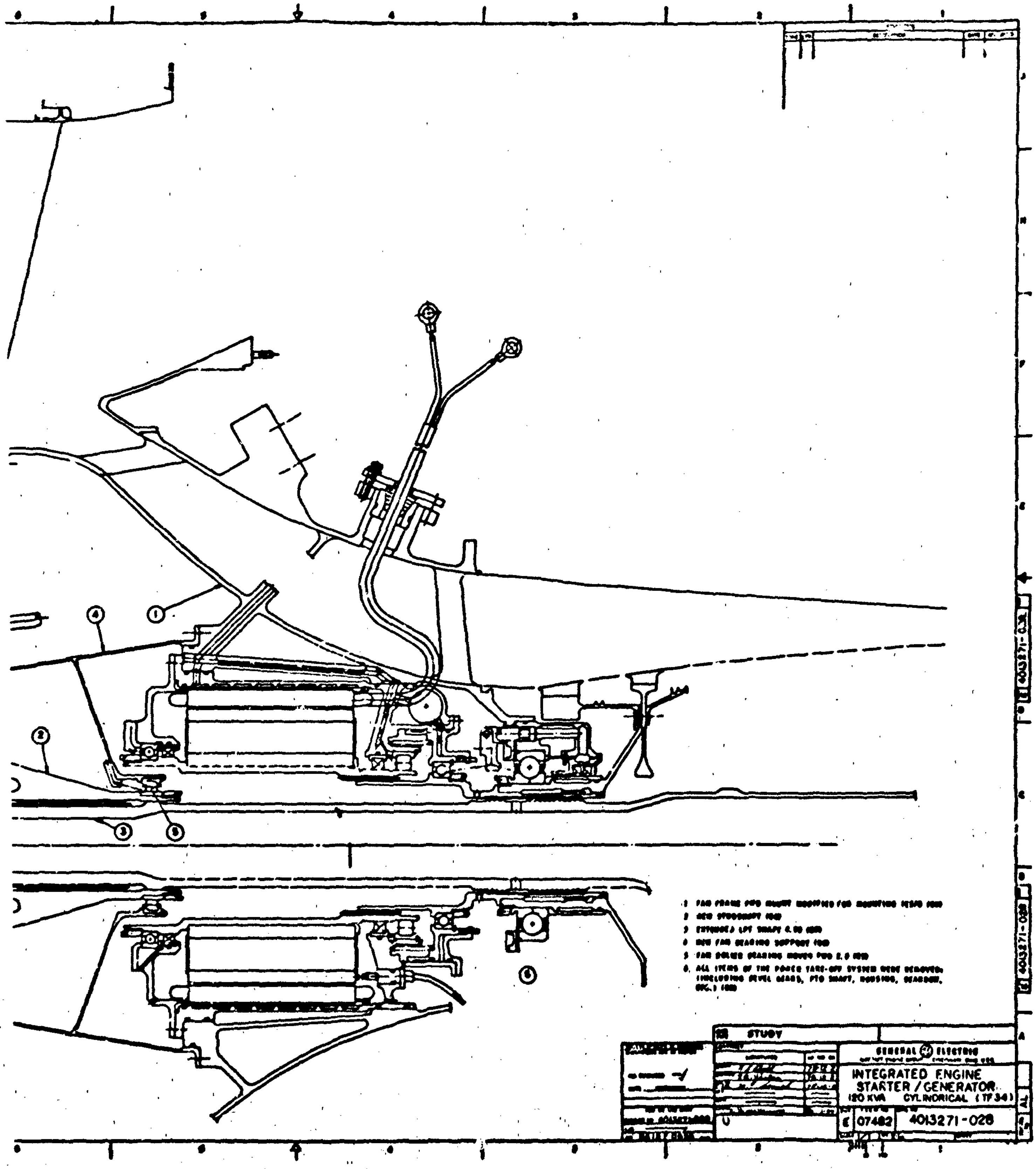
1
2
3
4
5
6



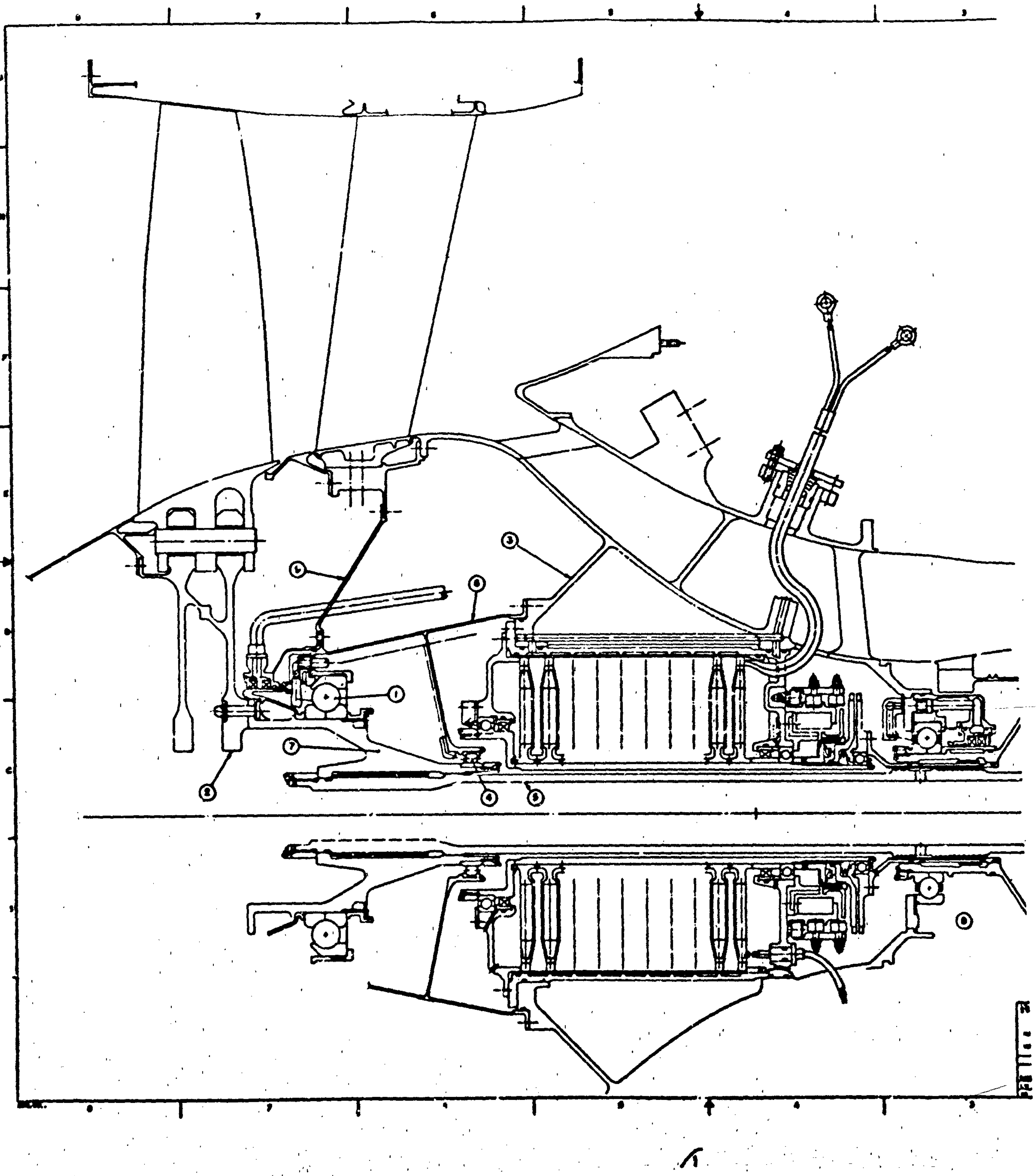
1 FAN FRAME FWD MOUNT MODIFIED FOR MOUNTING IES/G
2 NEW STUBSHAFT
3 EXTENDED LPT SHAFT
4 NEW FAN BEARING SUPPORT
5 FAN ROLLER BEARING MOVED FWD
6 ALL ITEMS OF THE POWER TAKE-OFF SYSTEM WERE REMOVED. (INCLUDING, BEVEL GEARS, PTO SHAFT, HOUSING, GEARBOX, ETC.)
STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER / GENERATOR
60 / 75 KVA 6 DISC (TF 34)
E
07482
4013271-027
4013271-027

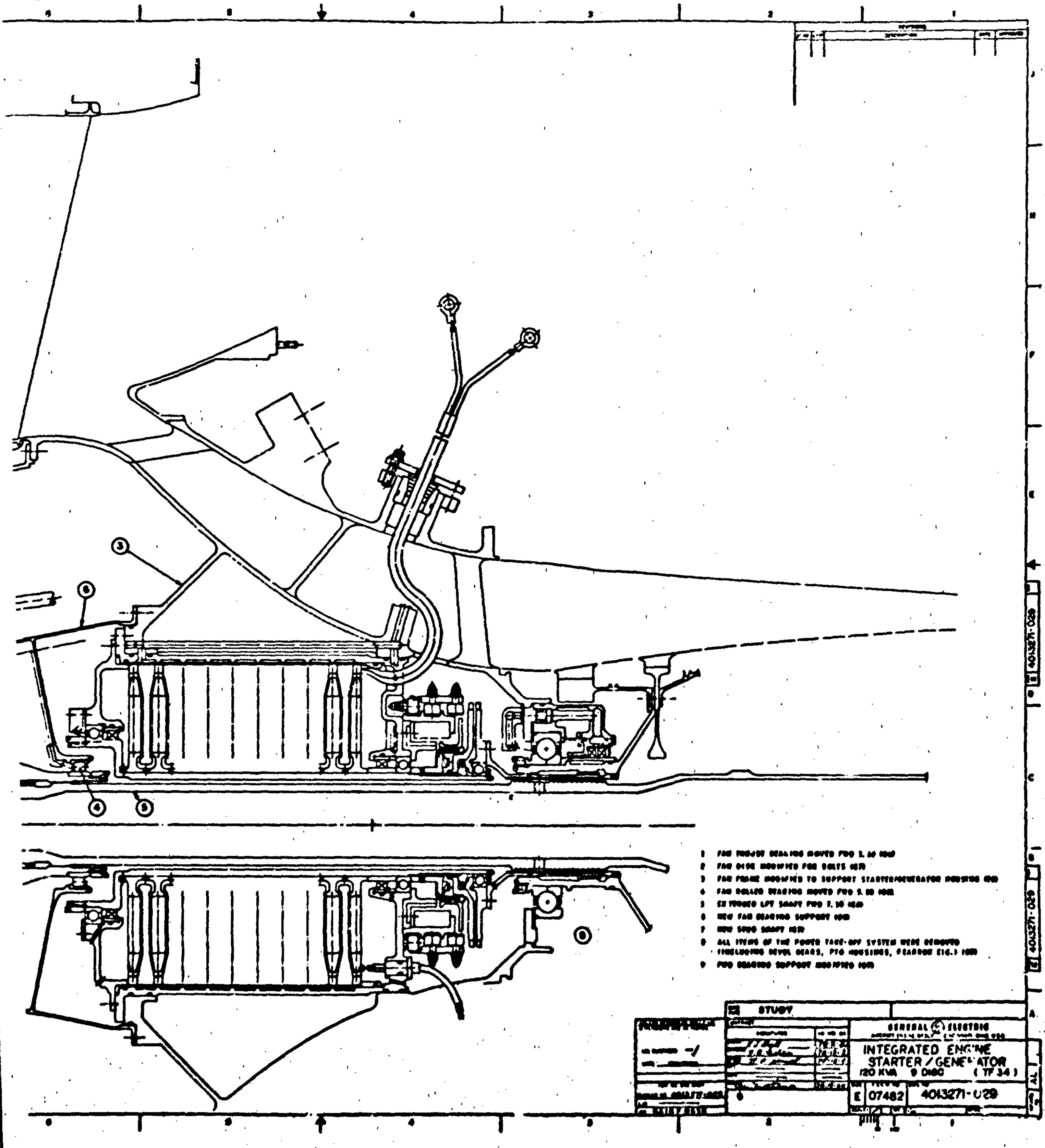


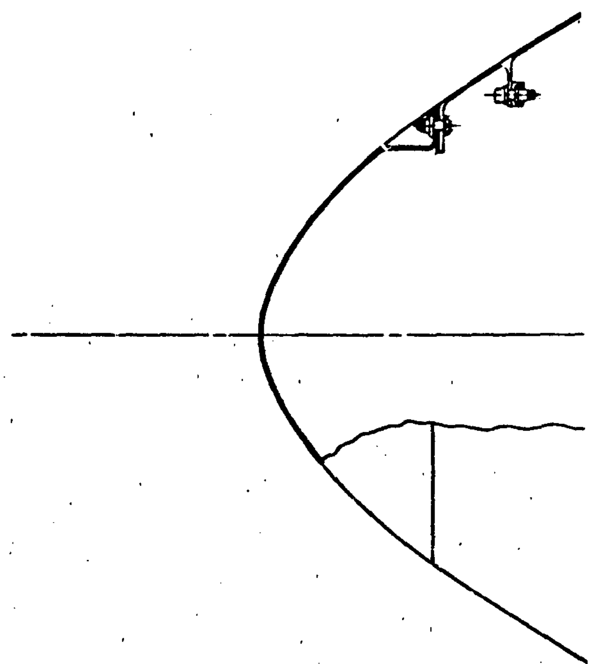
1
2
3
4
5
6

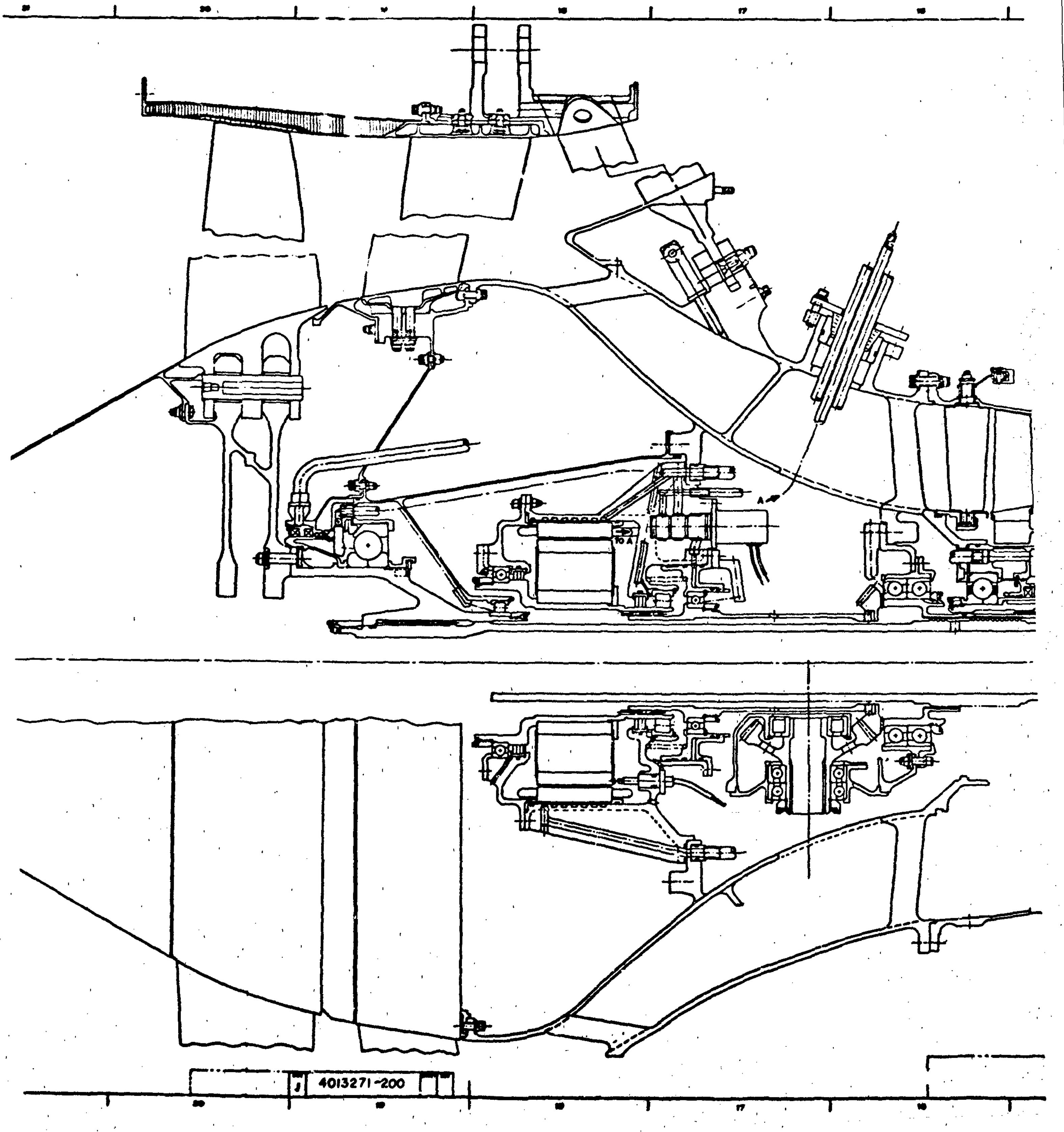


STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER / GENERATOR
120 KVA CYLINDRICAL (TF34)
07482
4013271-028

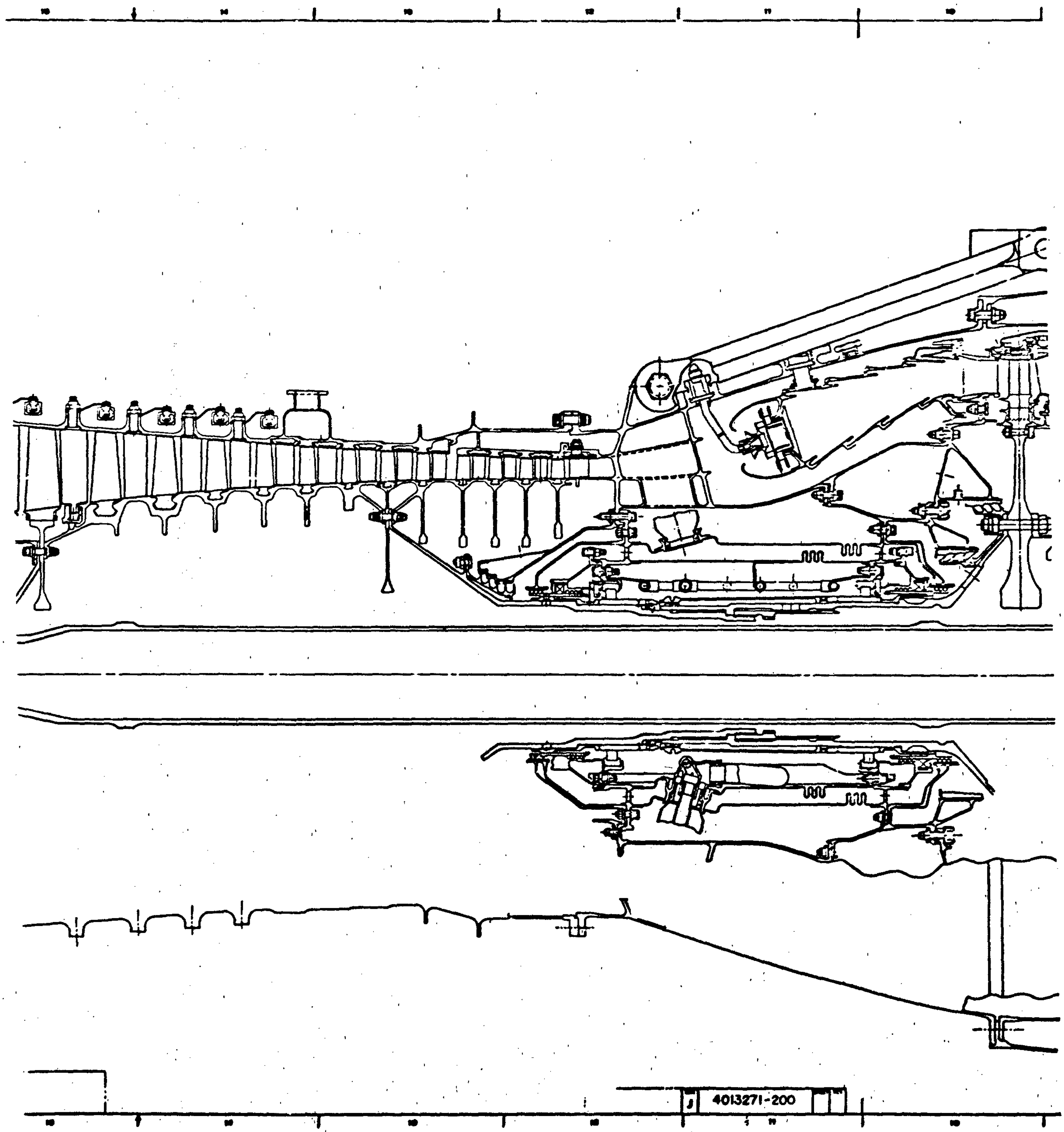



1 FAN THRUST BEARING MOVED FWD
2 FAN DISC MODIFIED FOR BOLTS
3 FAN FRAME MODIFIED TO SUPPORT STARTER/GENERATOR HOUSING
4 FAN ROLLER BEARING MOVED FWD
5 EXTENDED LPT SHAFT FWD
6 NEW FAN BEARING SUPPORT
7 NEW STUB SHAFT
8 ALL ITEMS OF THE POWER TAKE-OFF SYSTEM WERE REMOVED (INCLUDING BEVEL GEARS, PTO HOUSINGS, GEARBOX ETC.)
9 FWD BEARING SUPPORT MODIFIED
STUDY
GENERAL ELECTRIC
INTEGRATED ENGINE STARTER / GENERATOR
120 KVA 9 DISC (TF 34)
E 07482 4013271-029
4013271-029





TO A
A
J
4013271-200


J
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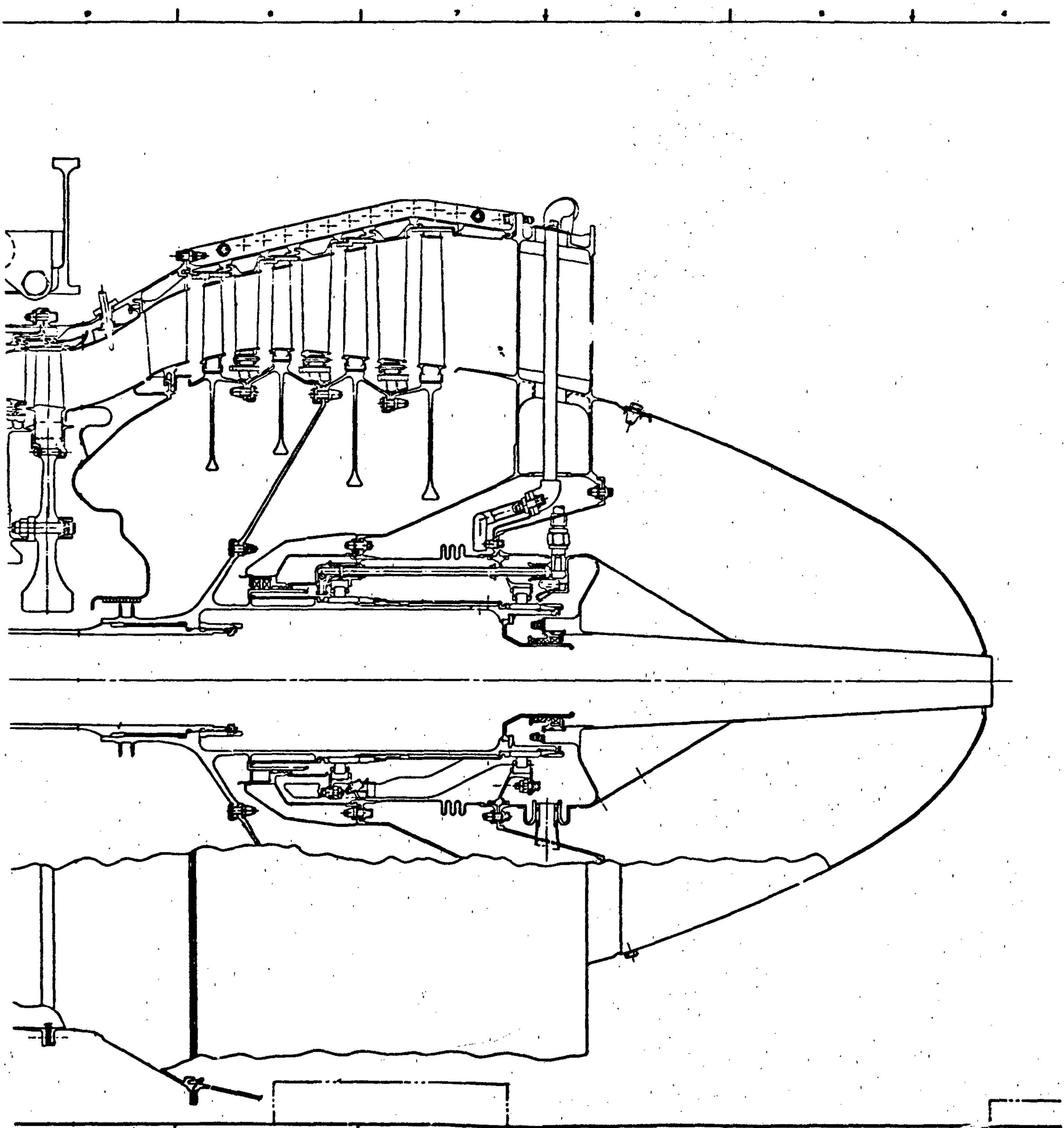
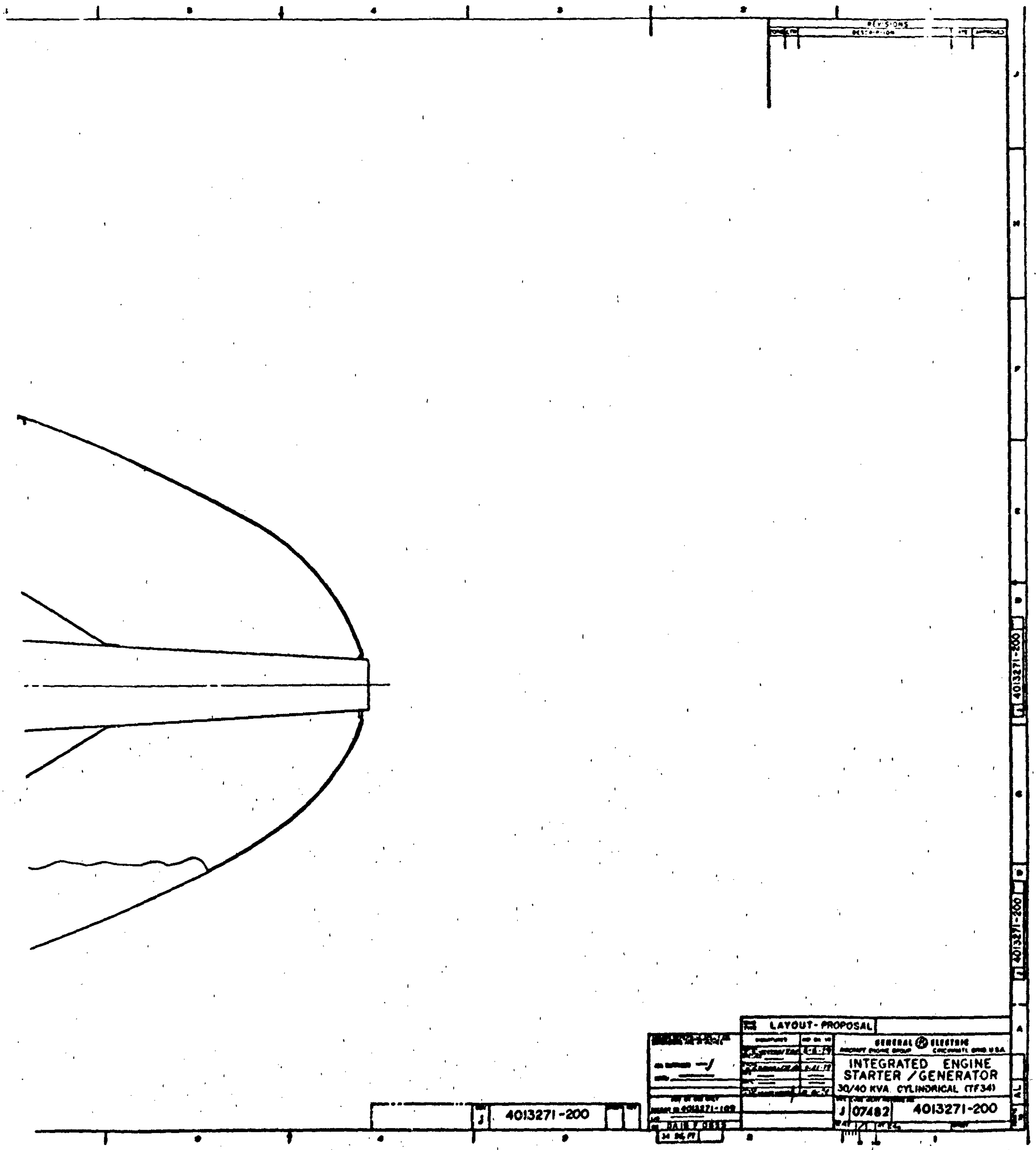
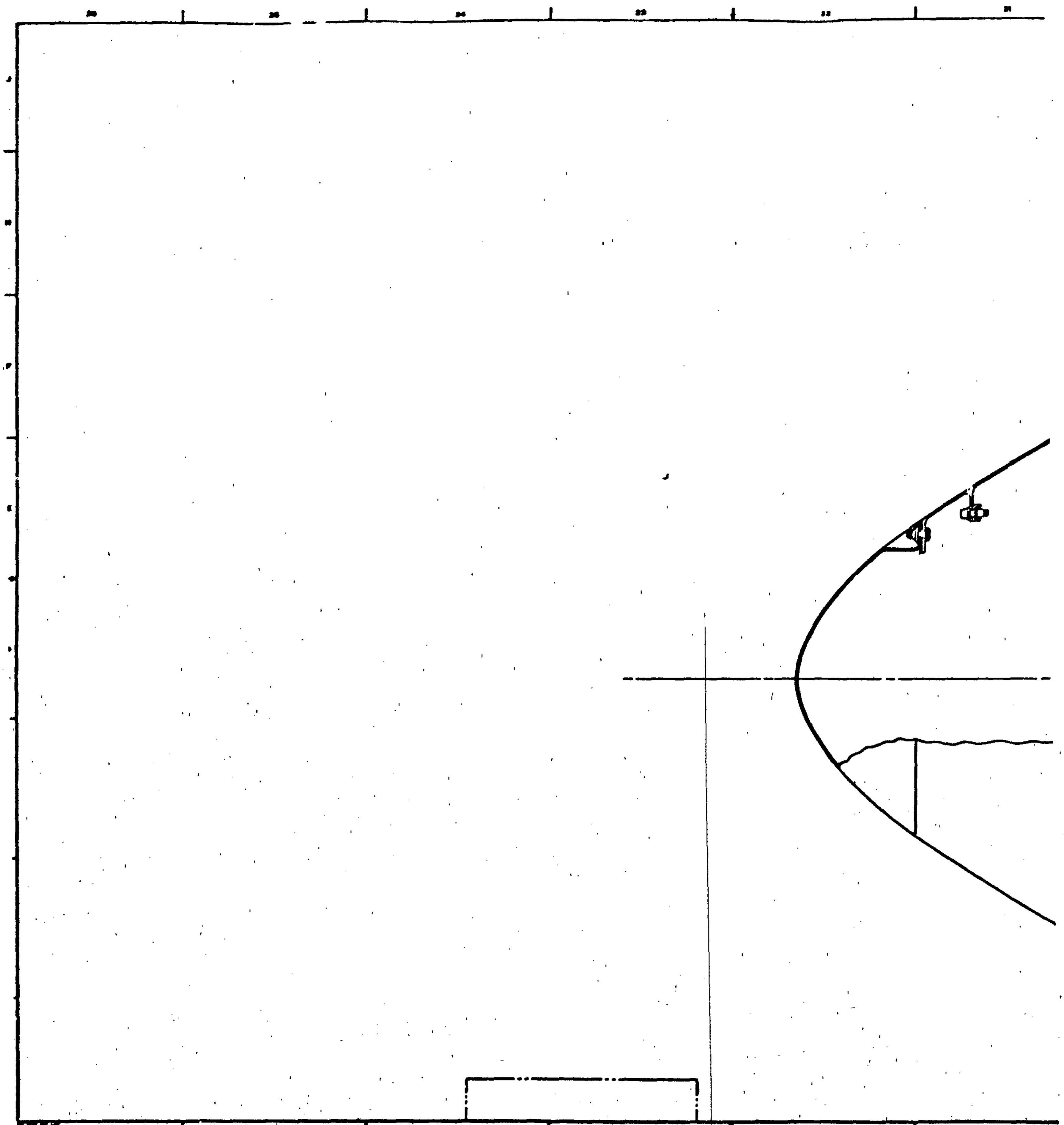
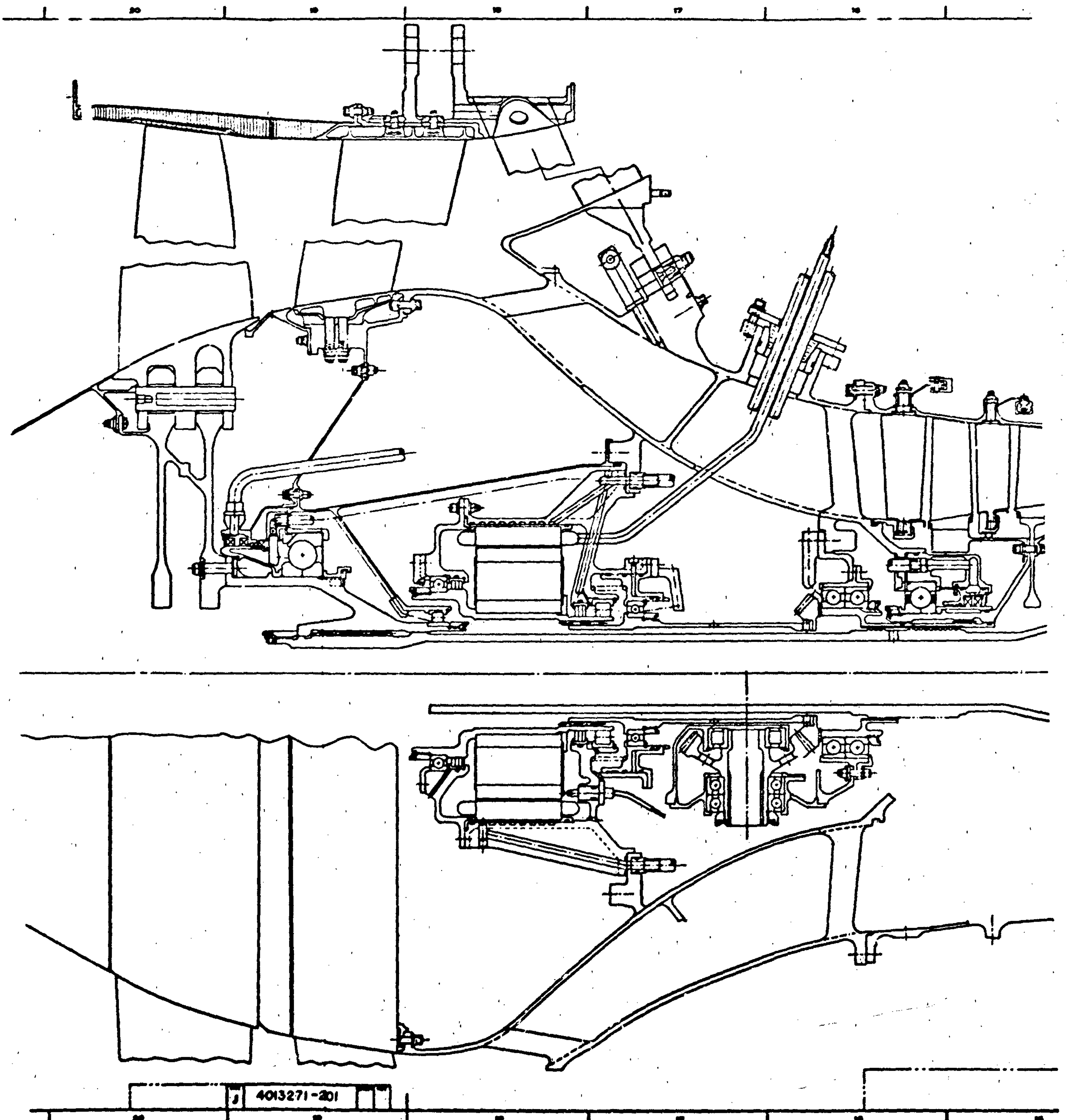


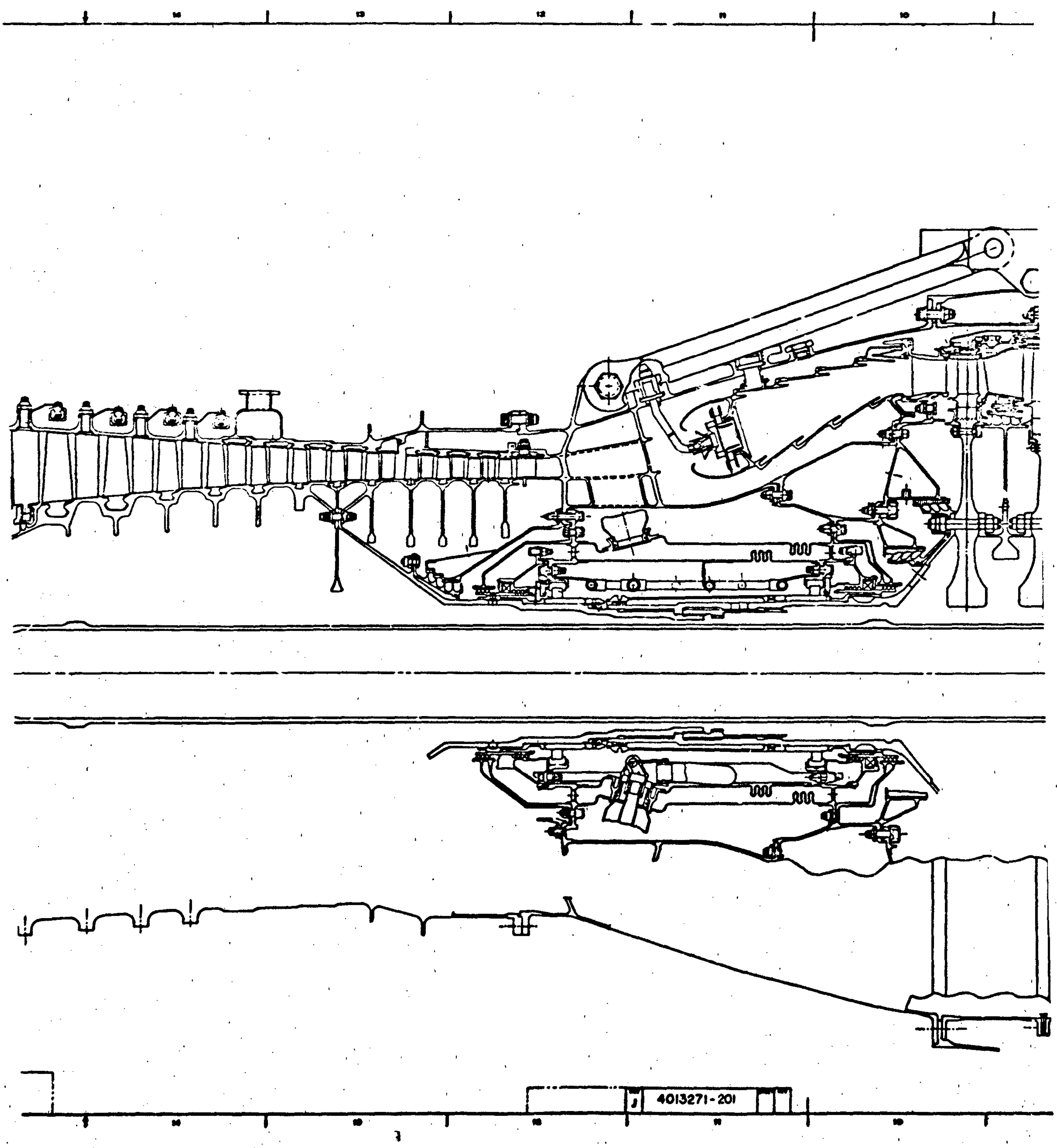


4013271-200 AD

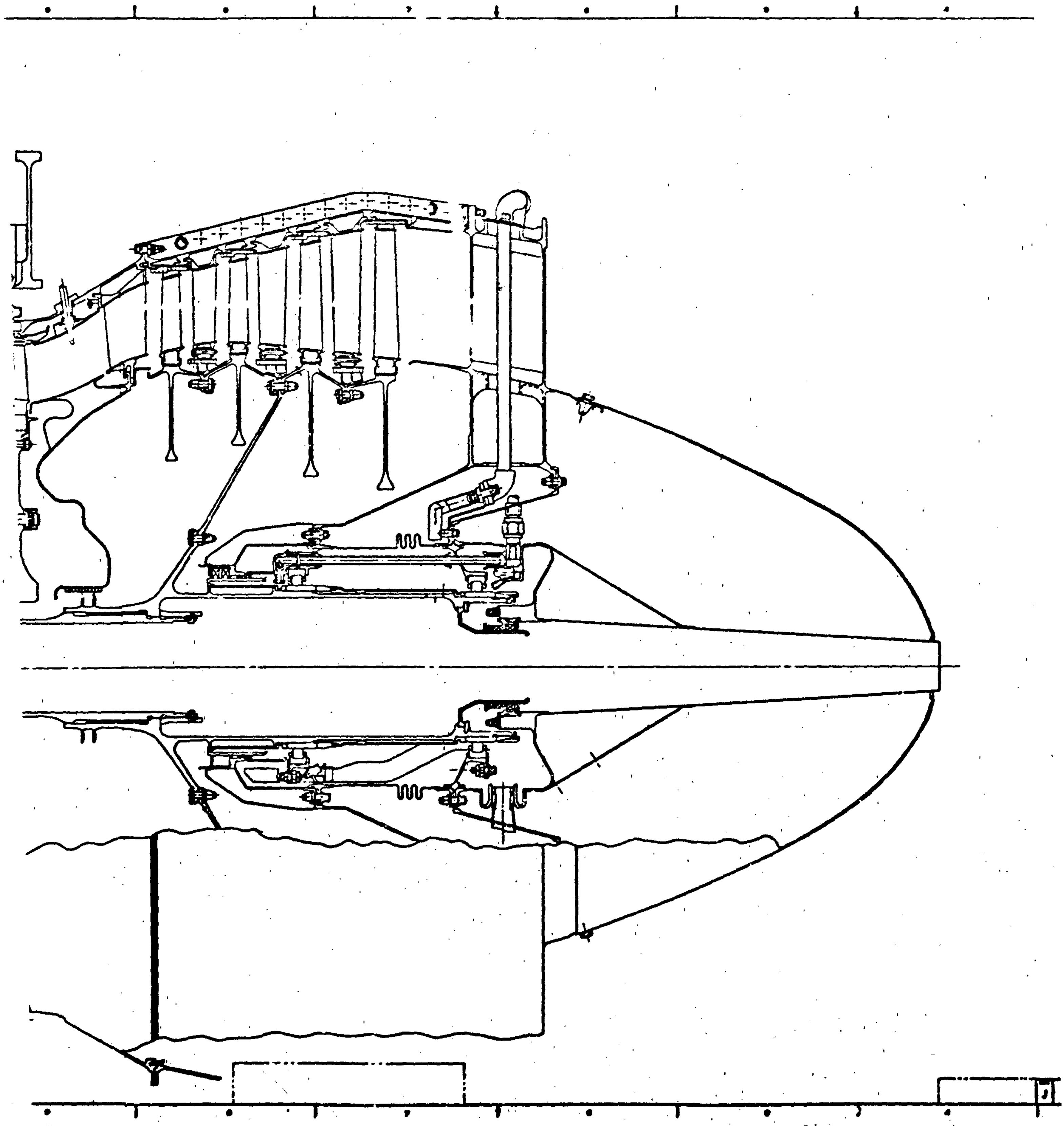


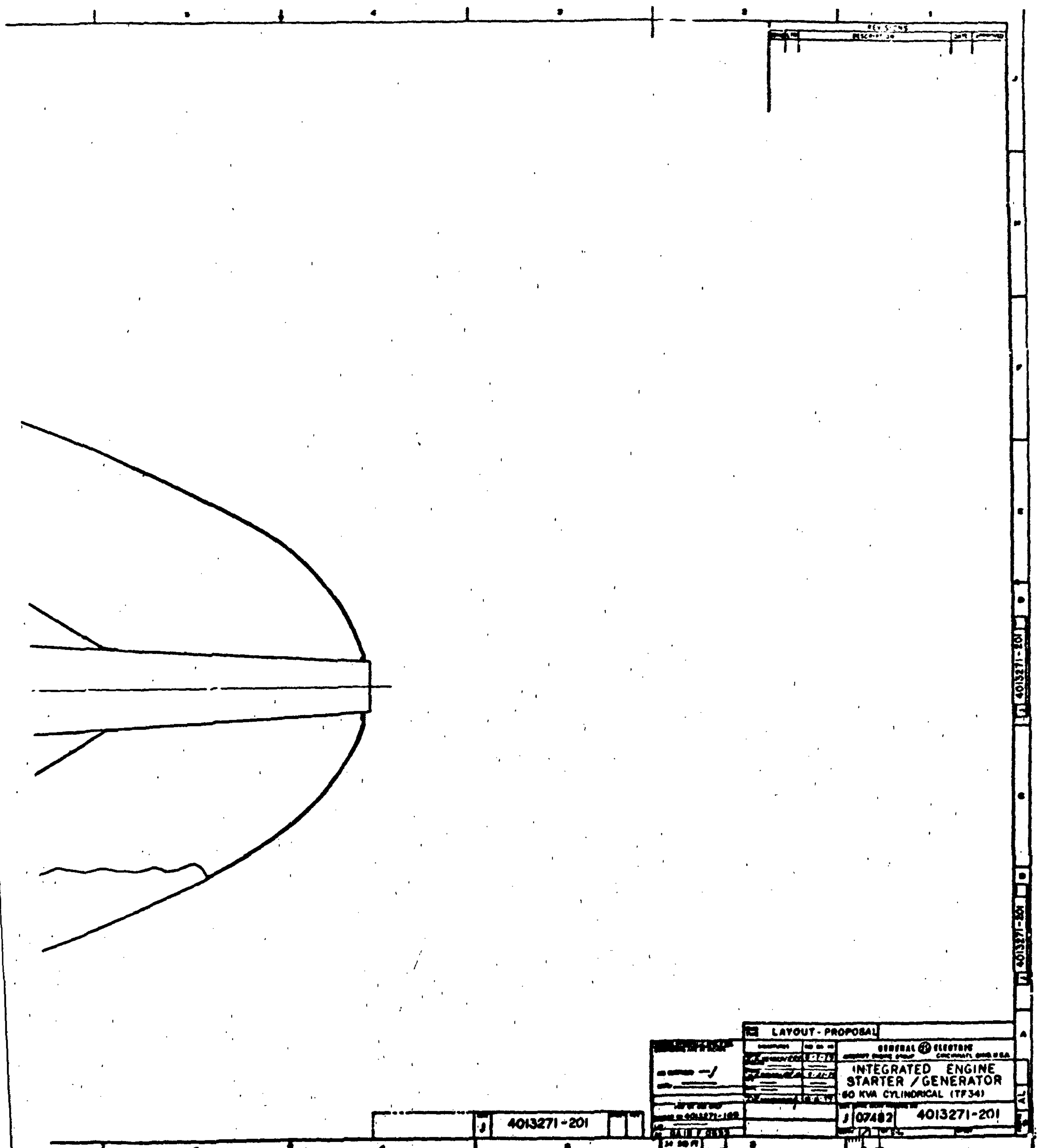


J
4013271-201

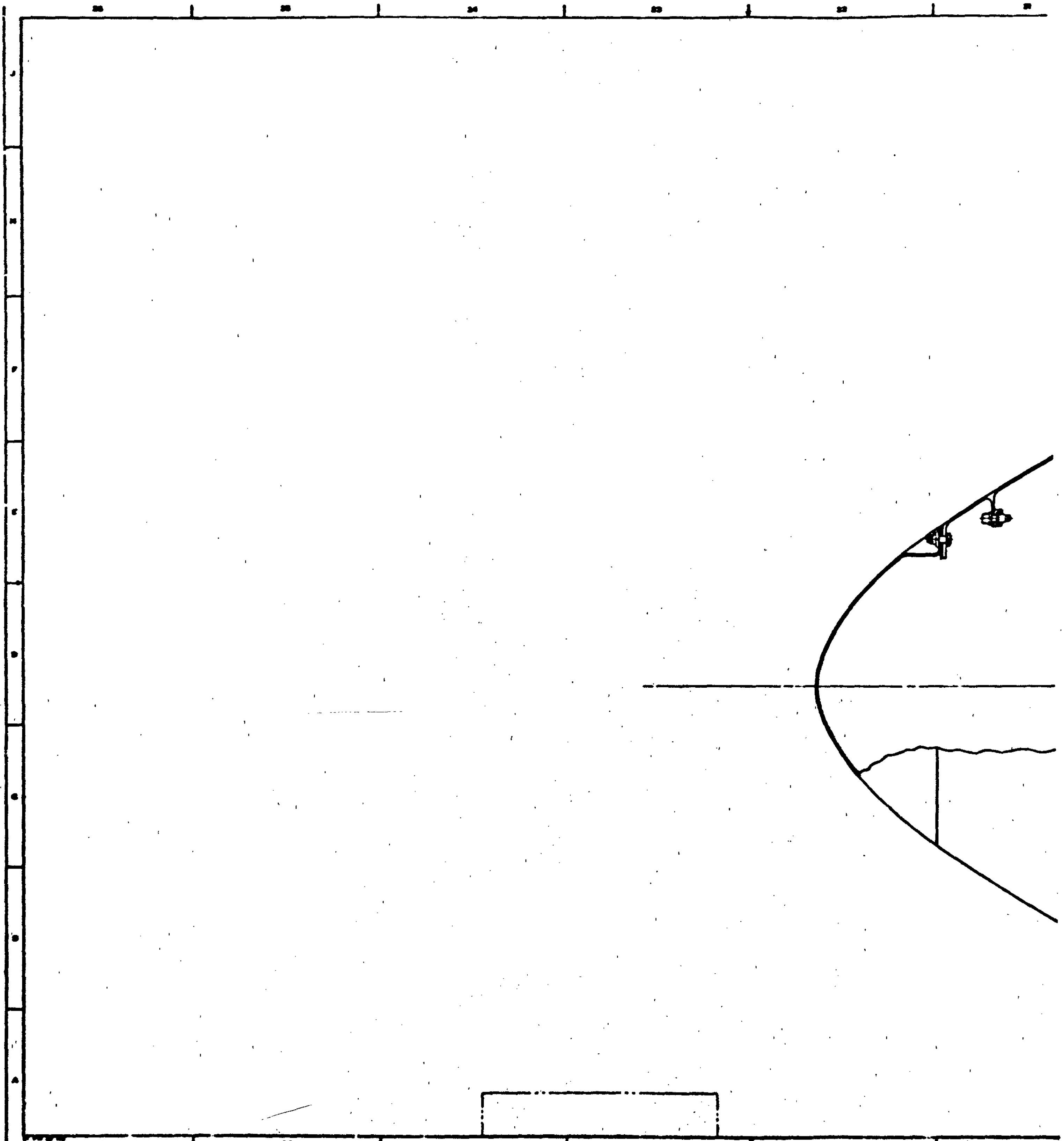


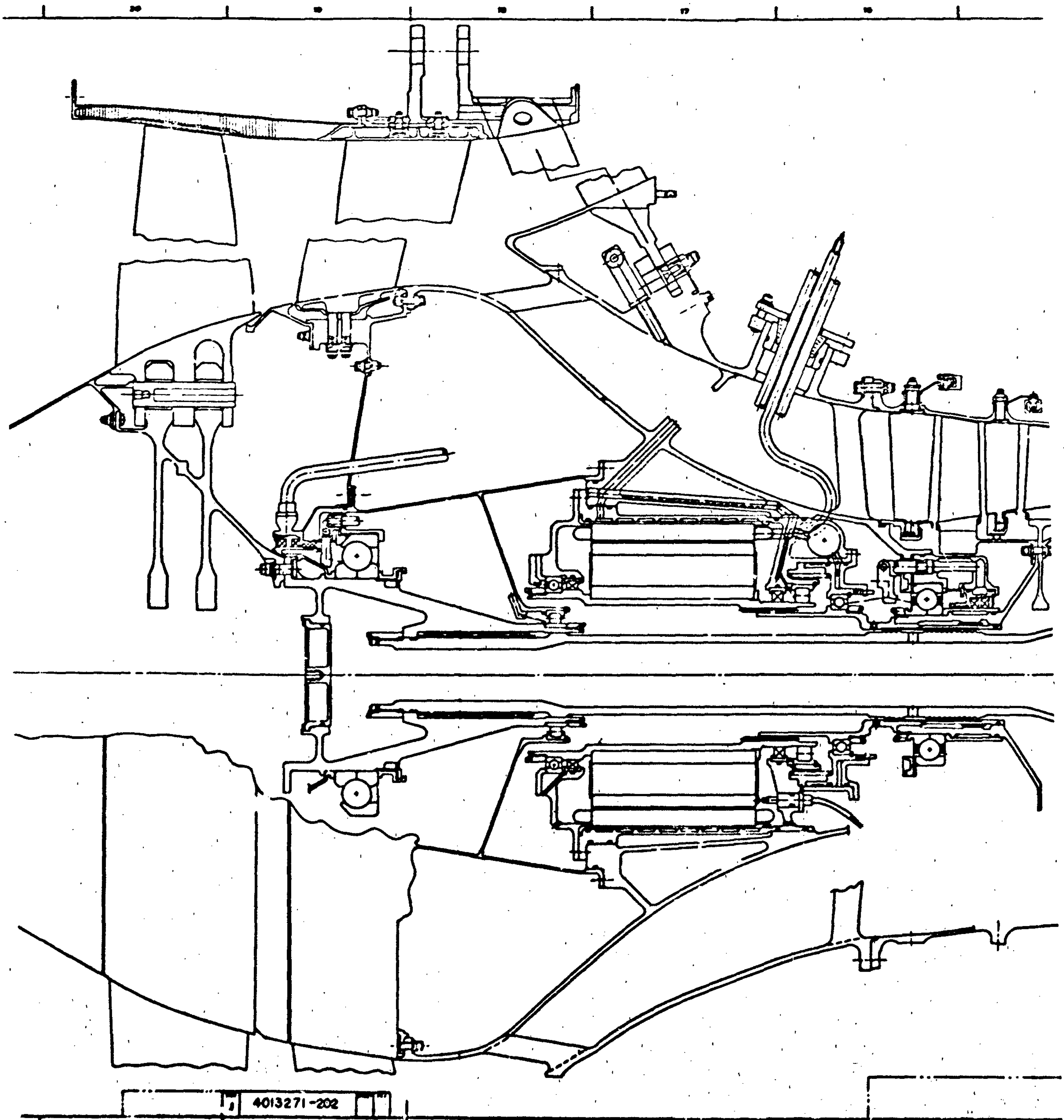
2

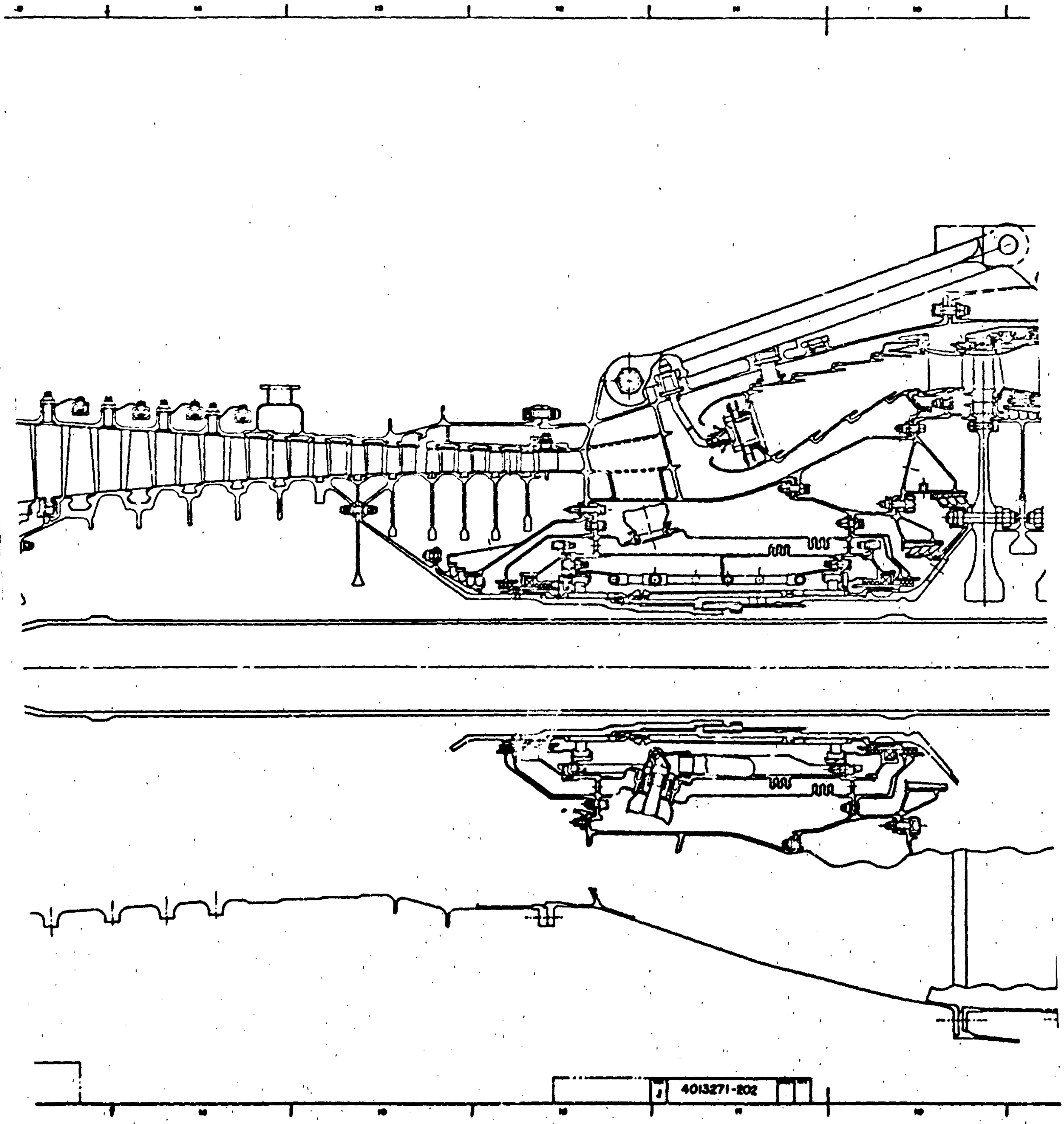




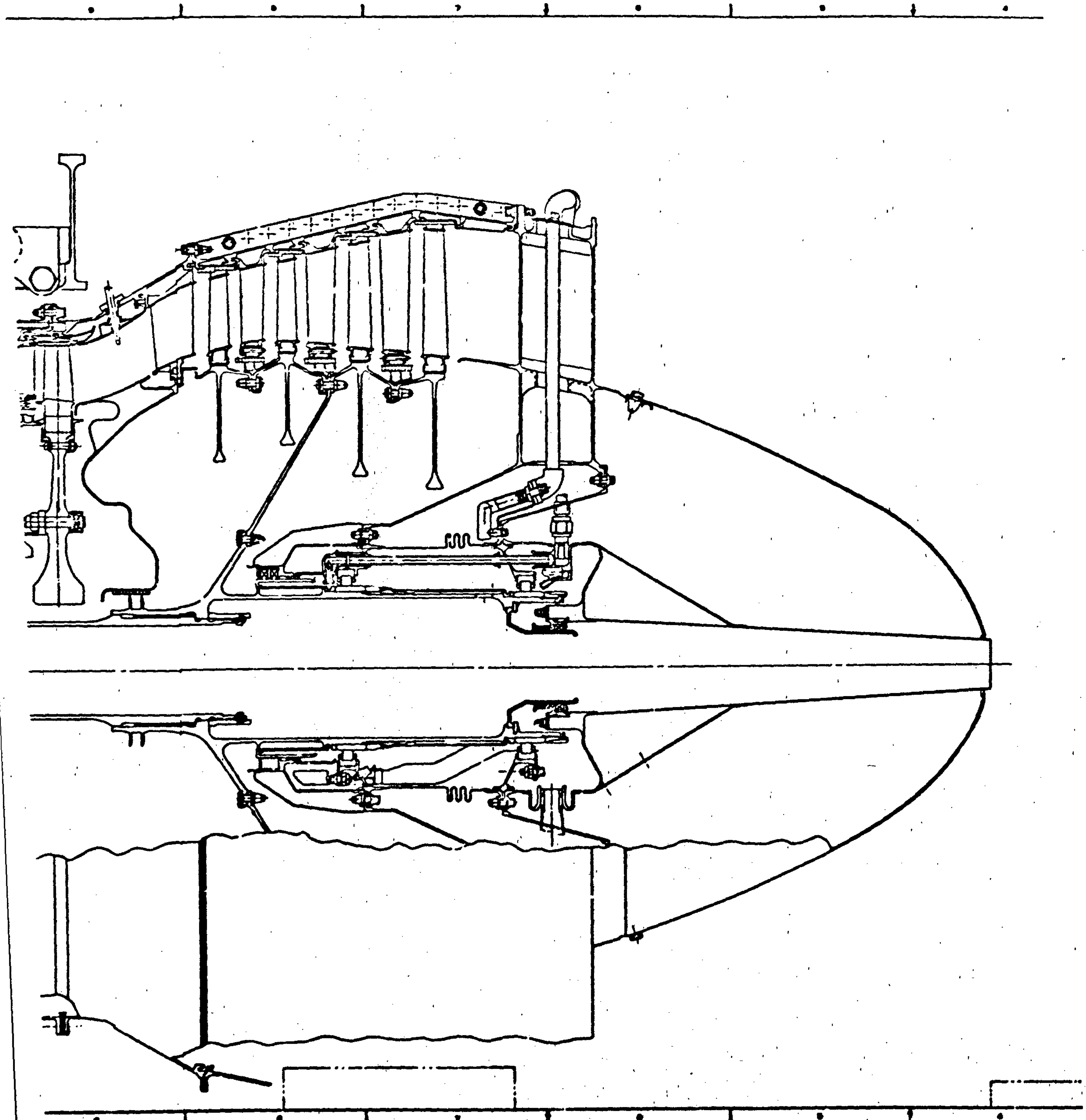
LAYOUT - PROPOSAL
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER / GENERATOR
60 KVA CYLINDRICAL (TF34)
J 07482 4013271-201
4013271-201

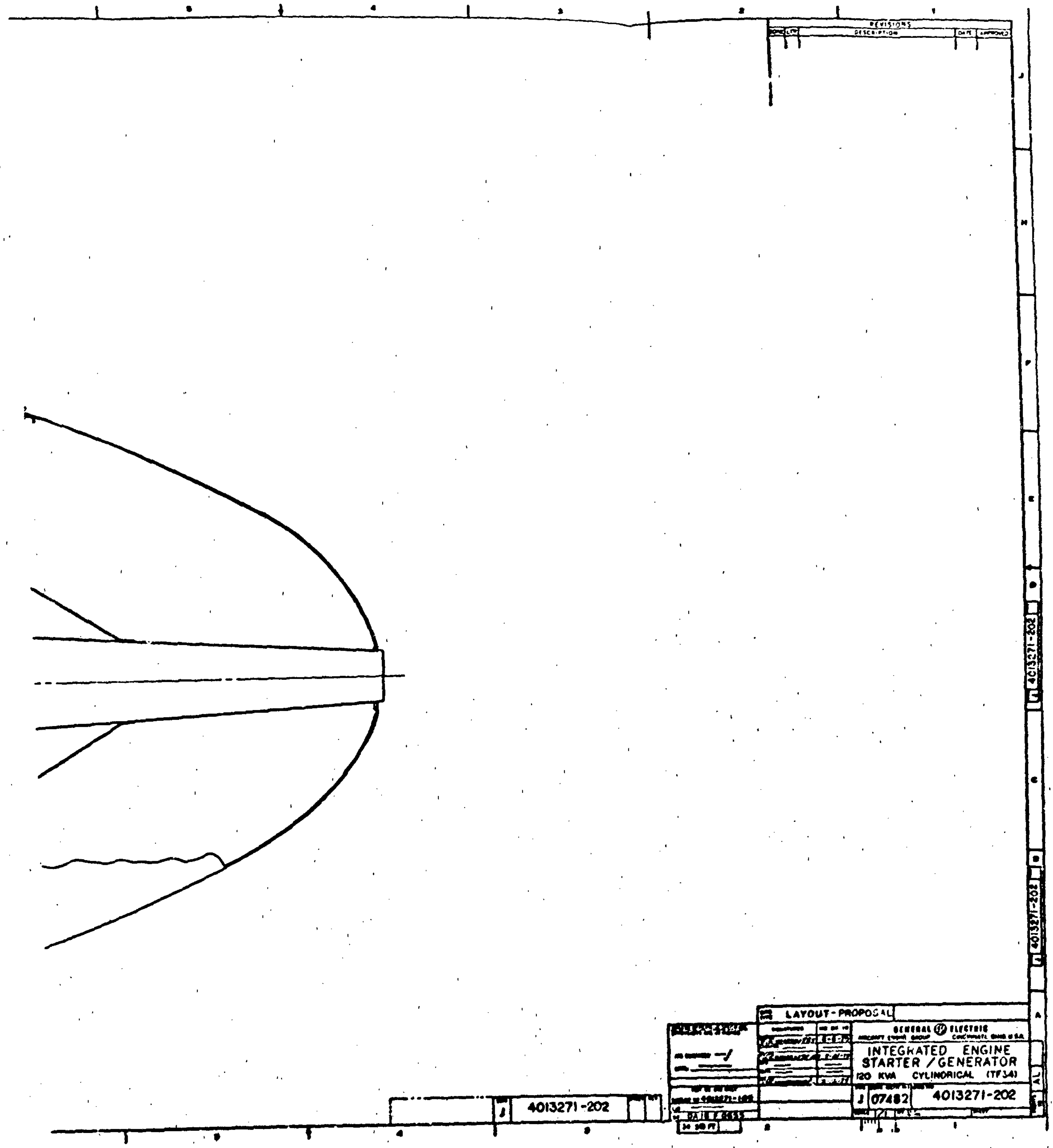



4013271-202

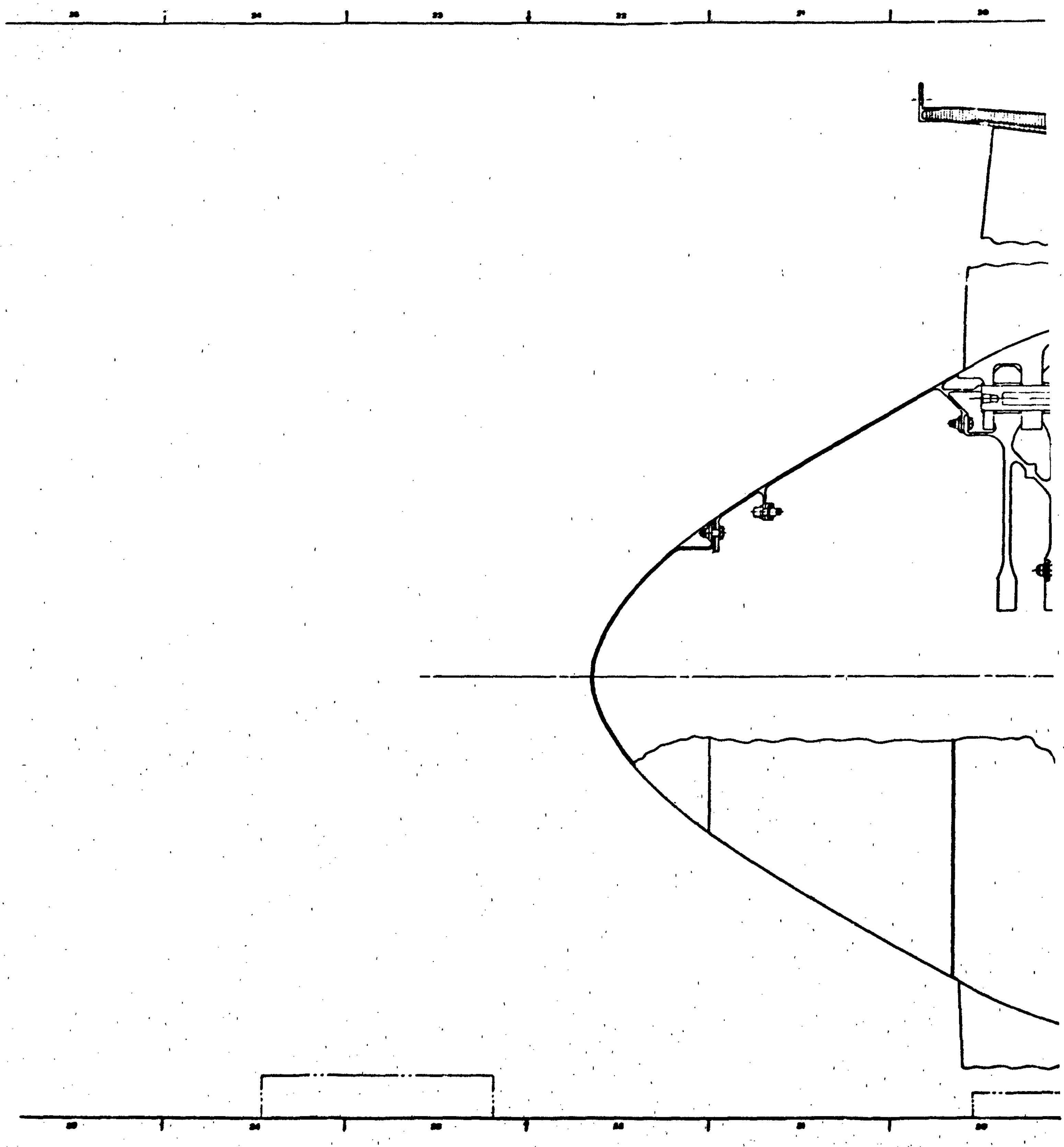


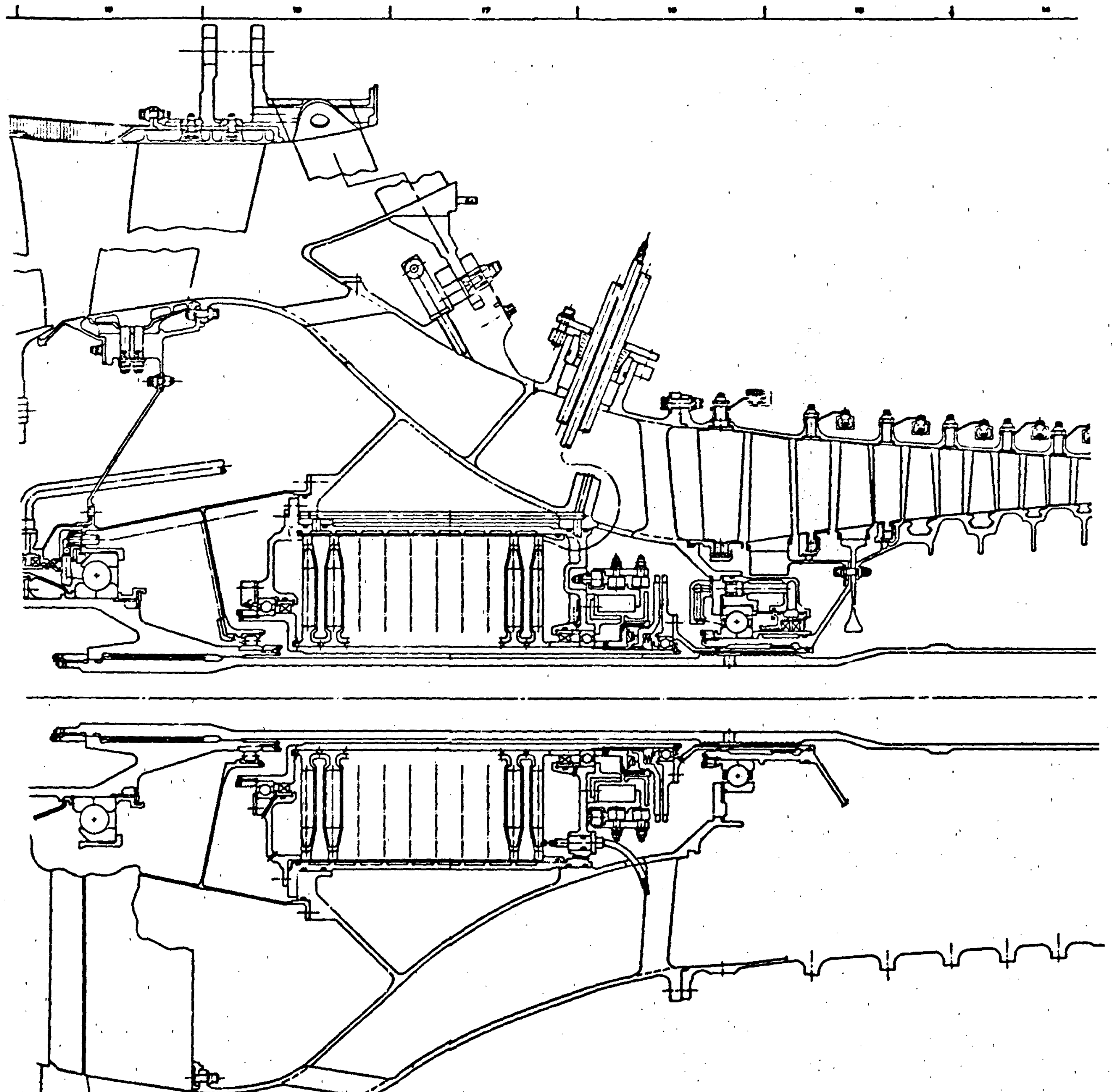
1 3



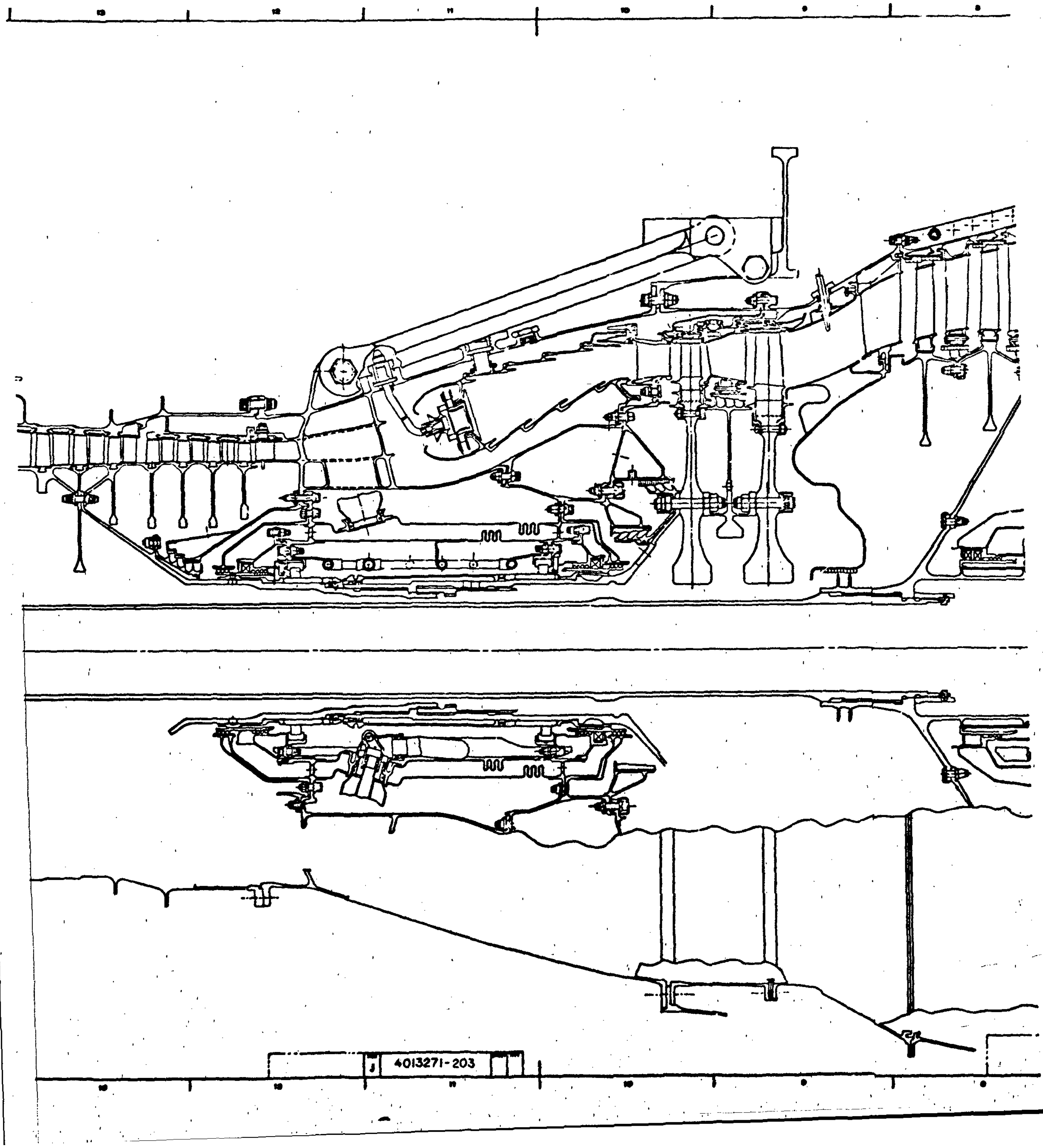


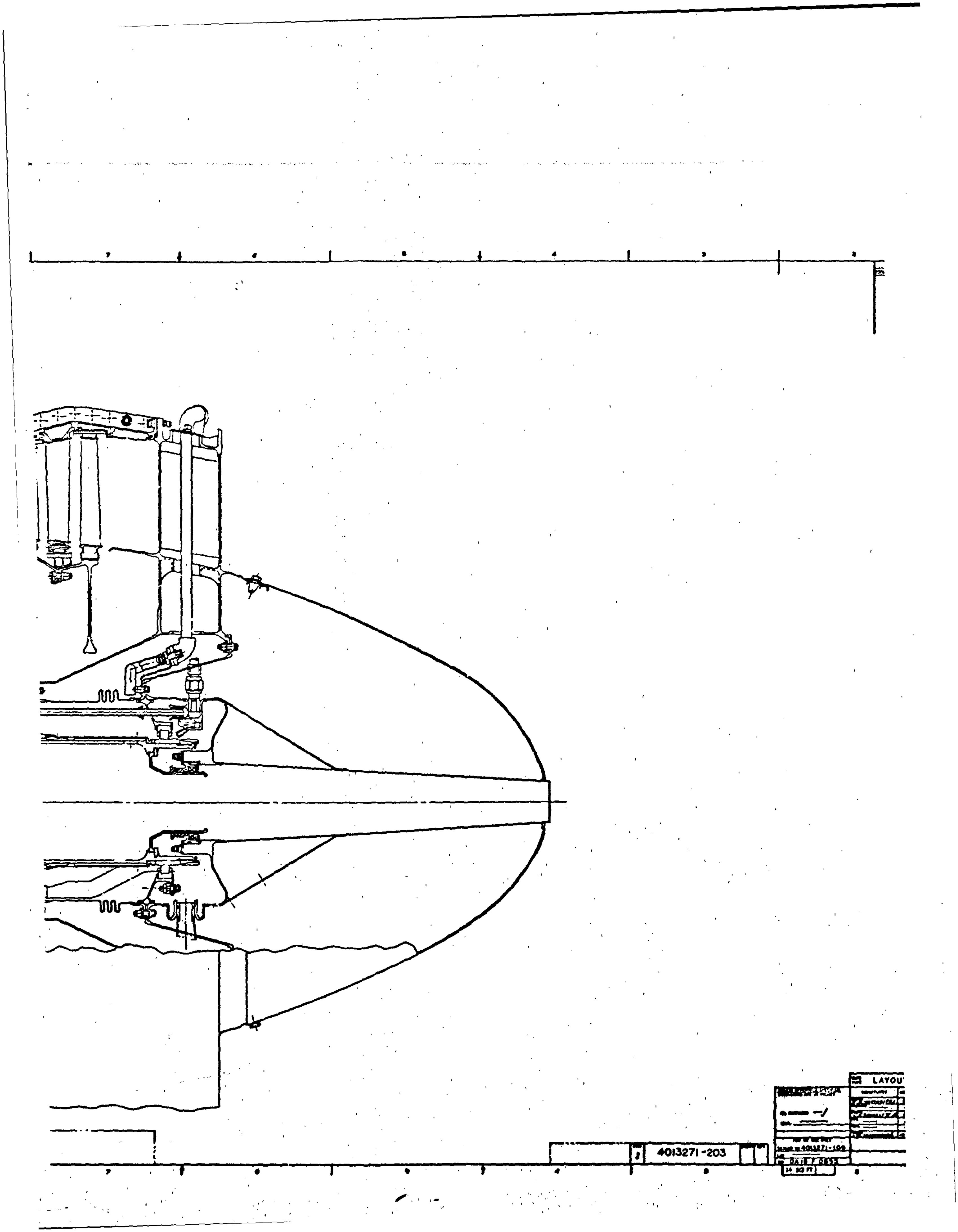
AD 4013271-202



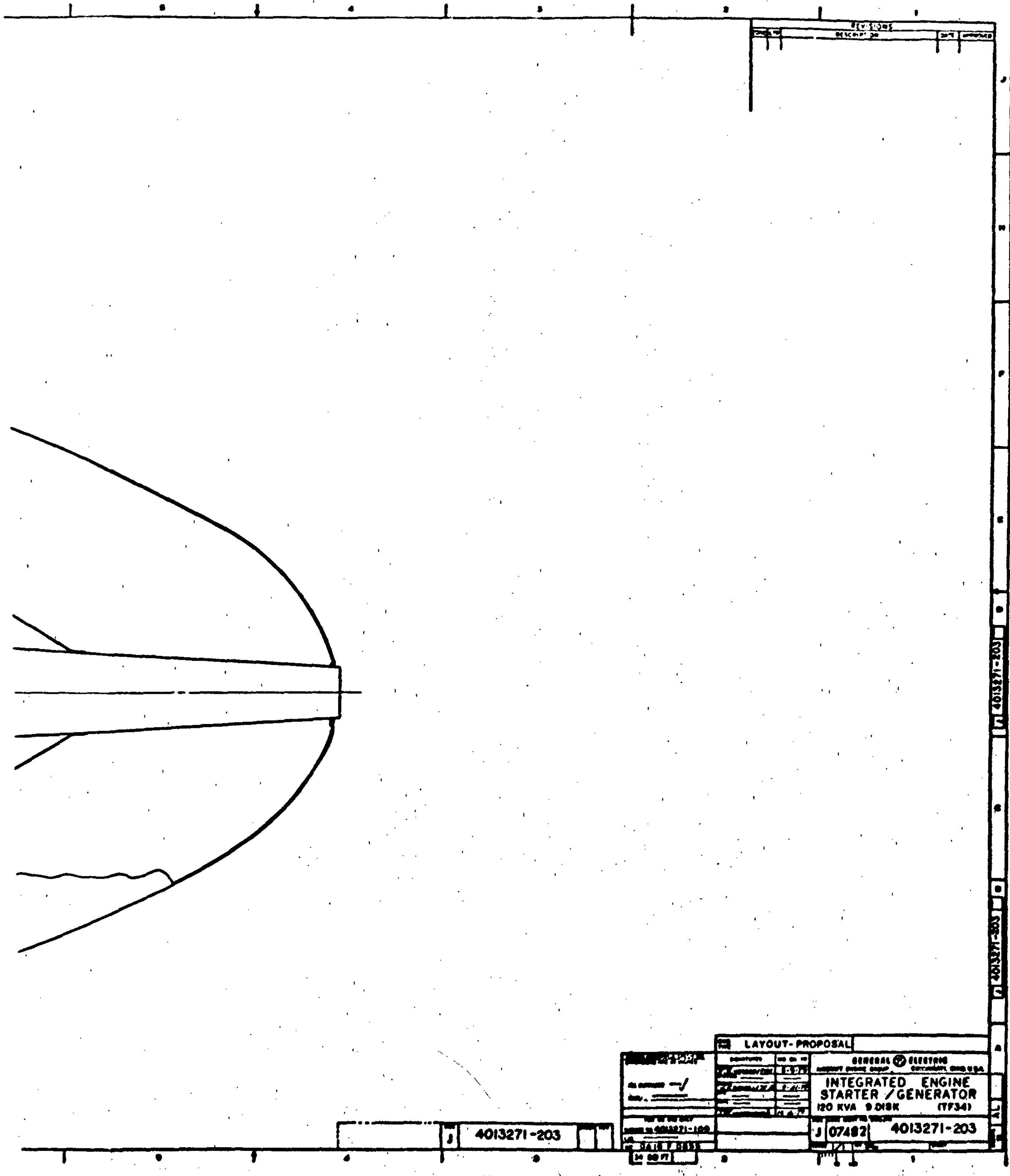


4013271-203

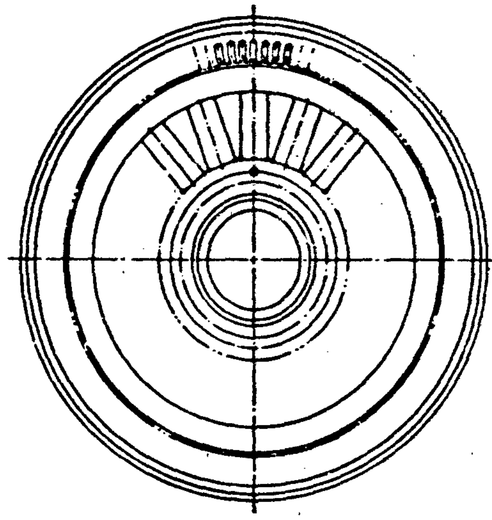

J
4013271-203



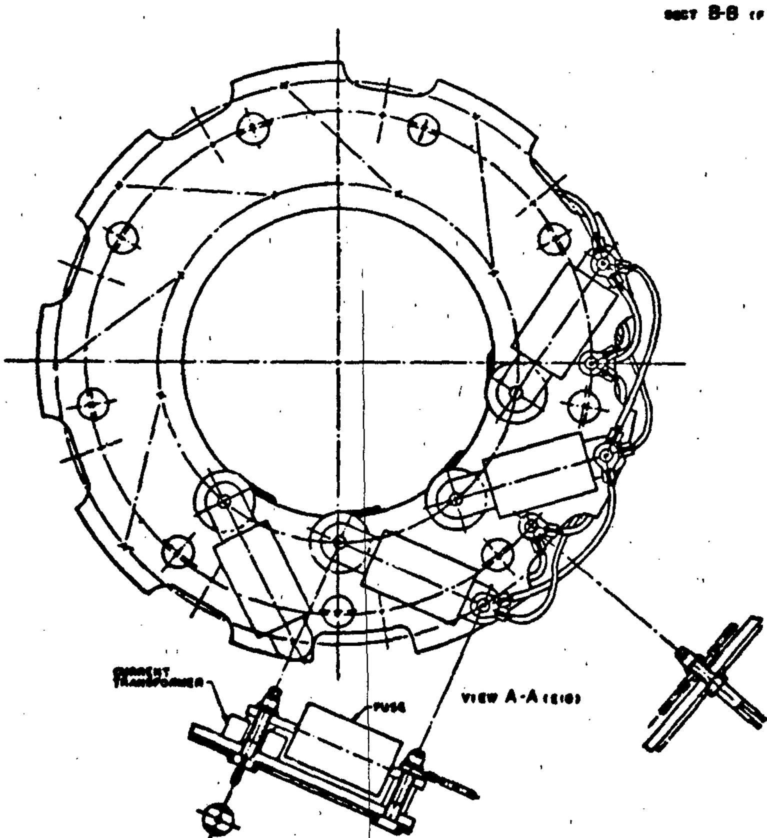
LAYOU
4013271-203
4013271-109
14 SQ FT



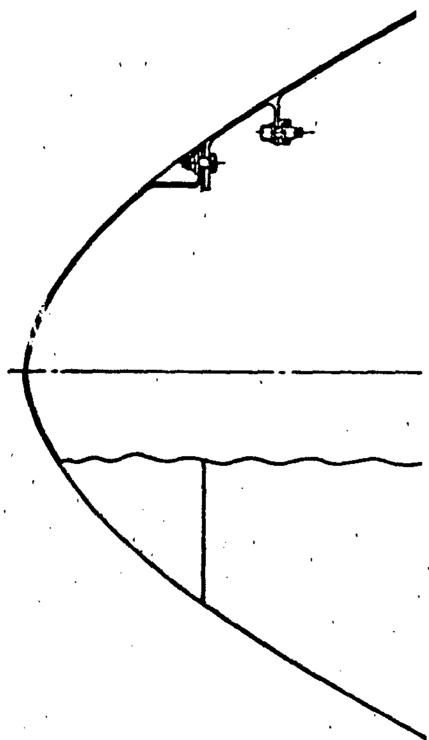
AD 4013271-203



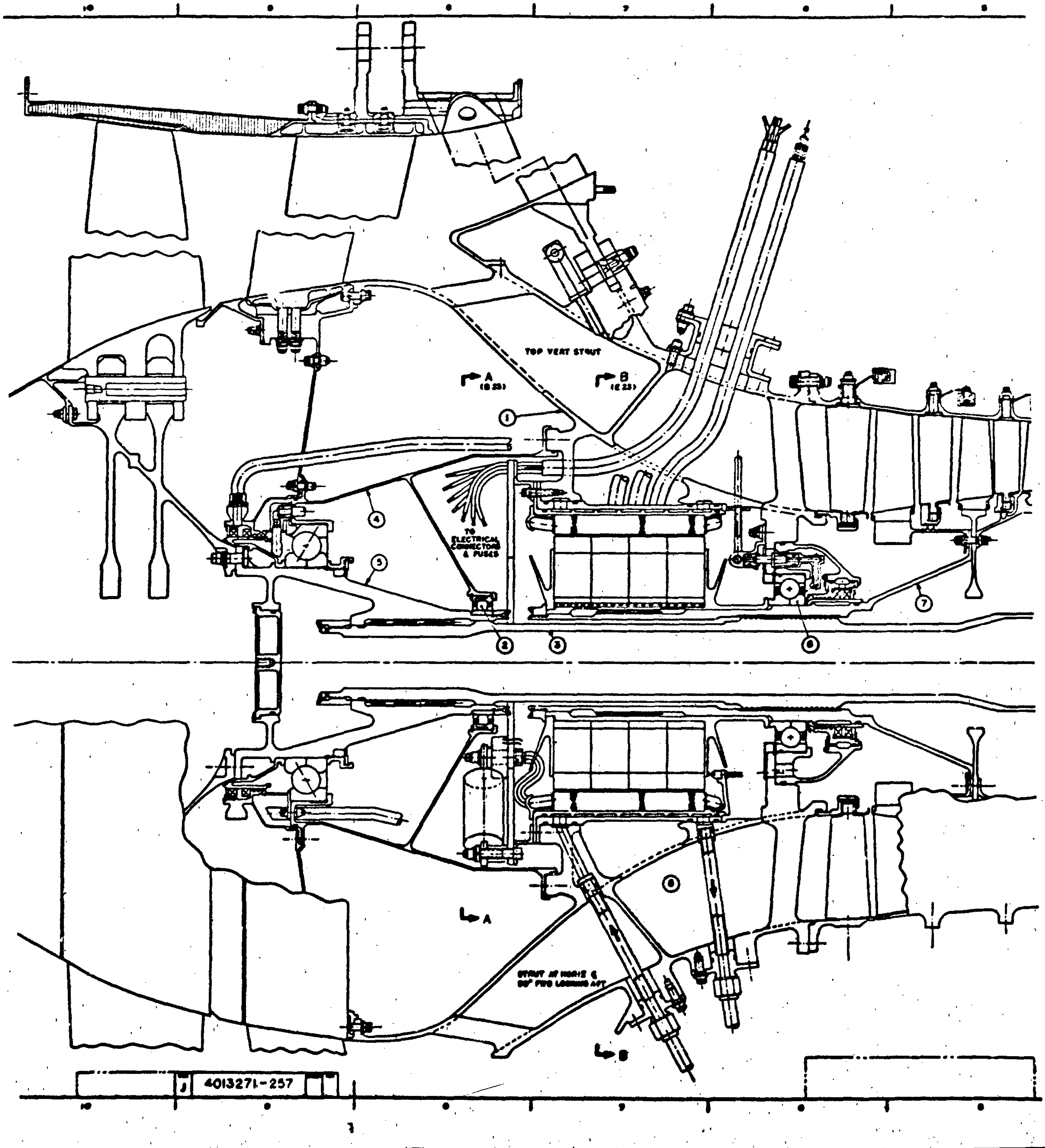
SECT B-B (F17)



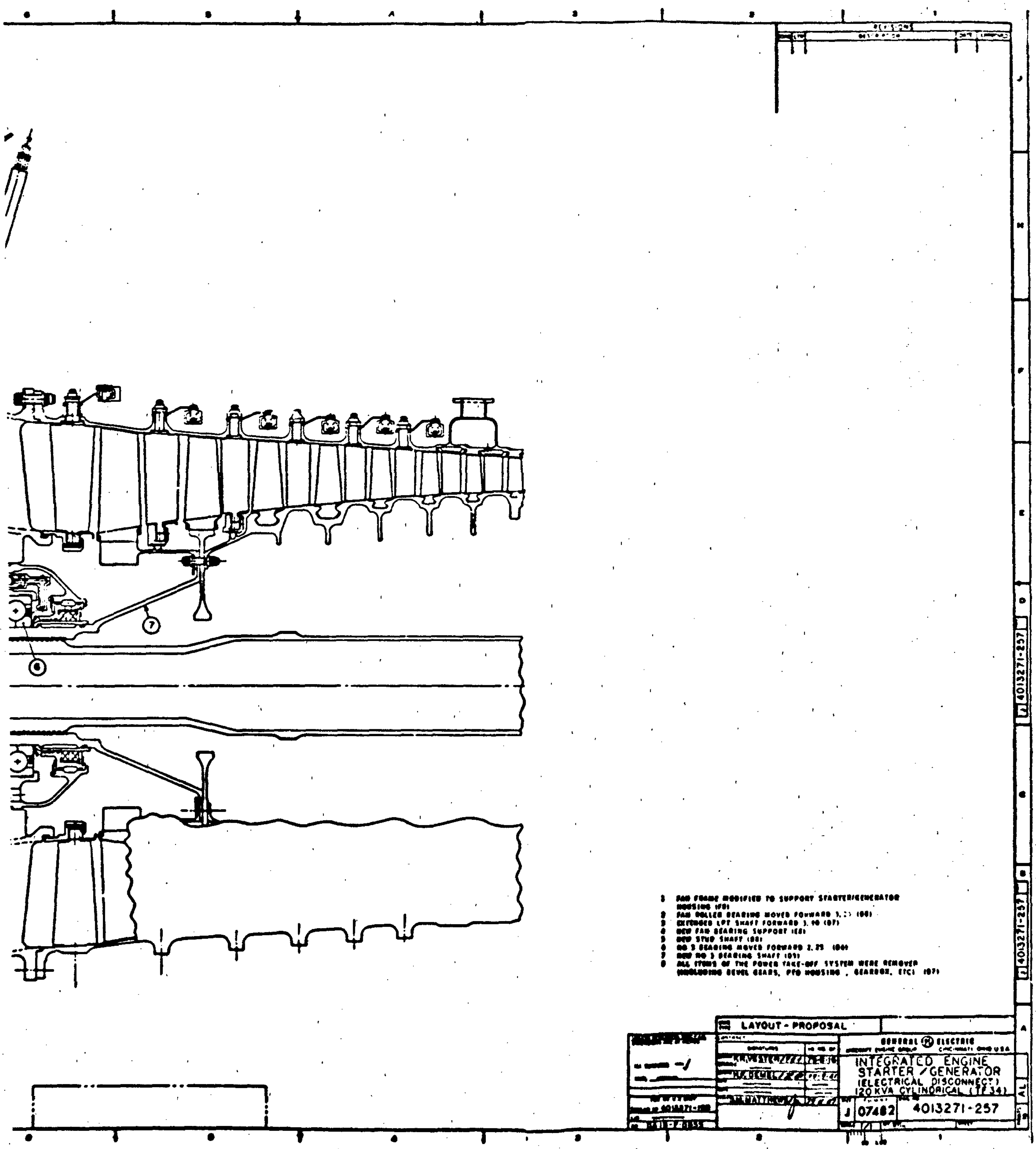
VIEW A-A

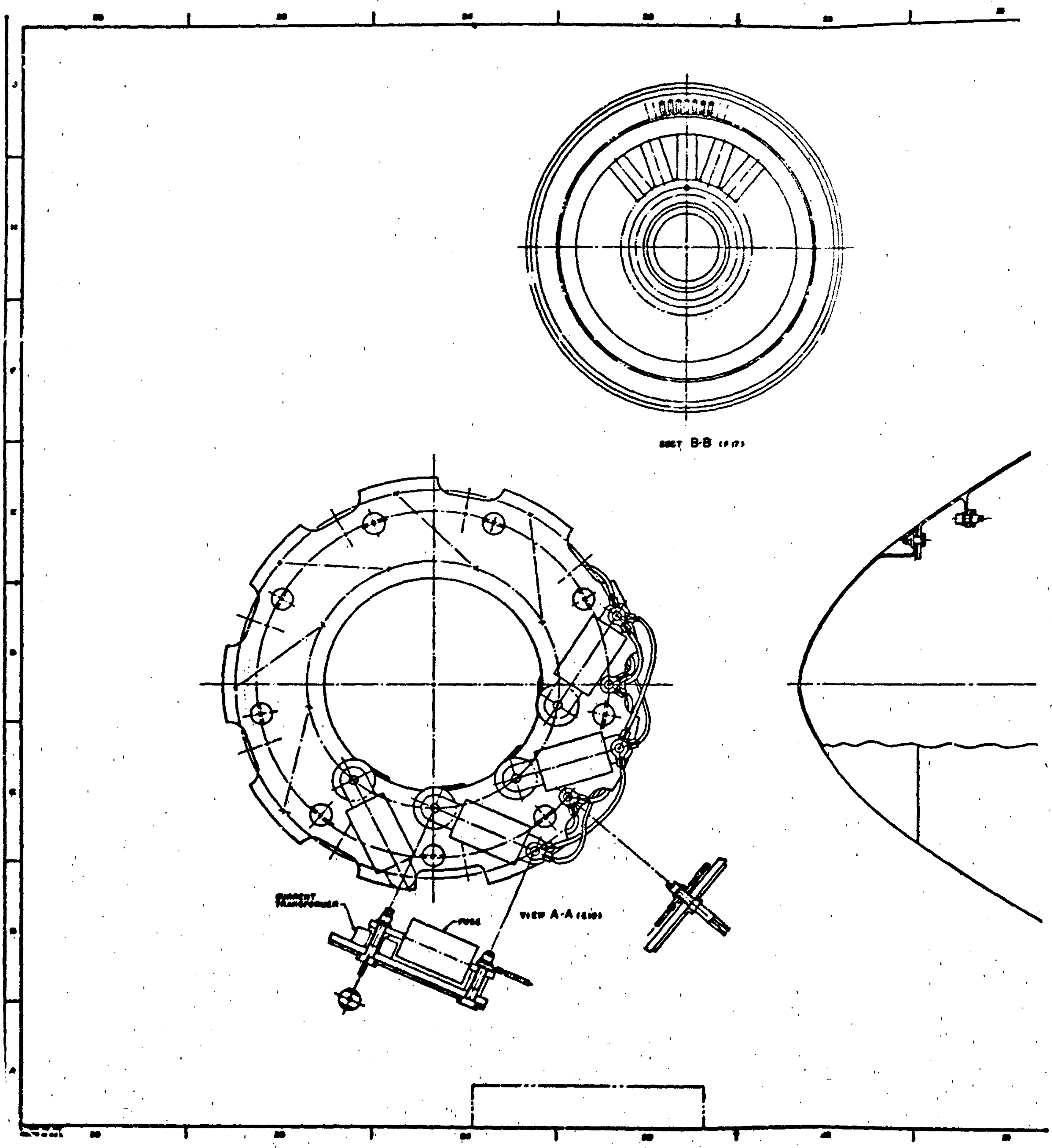


rr

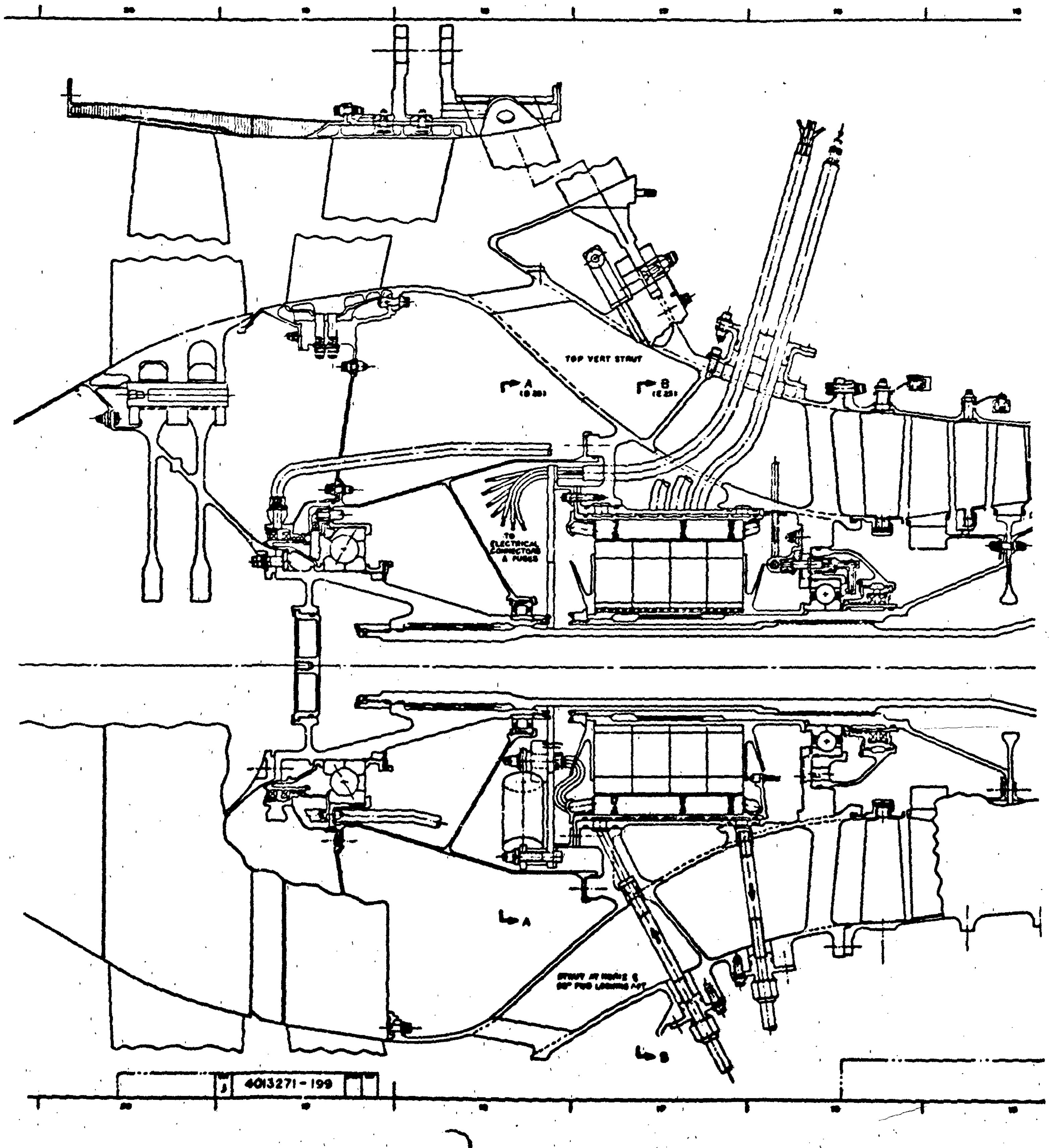


TOP VERT STRUT
A
(E 23)
B
(E 23)
1
4
5
TO ELECTRICAL CONNECTORS & FUSES
2
3
6
7
8
A
B
4013271-257

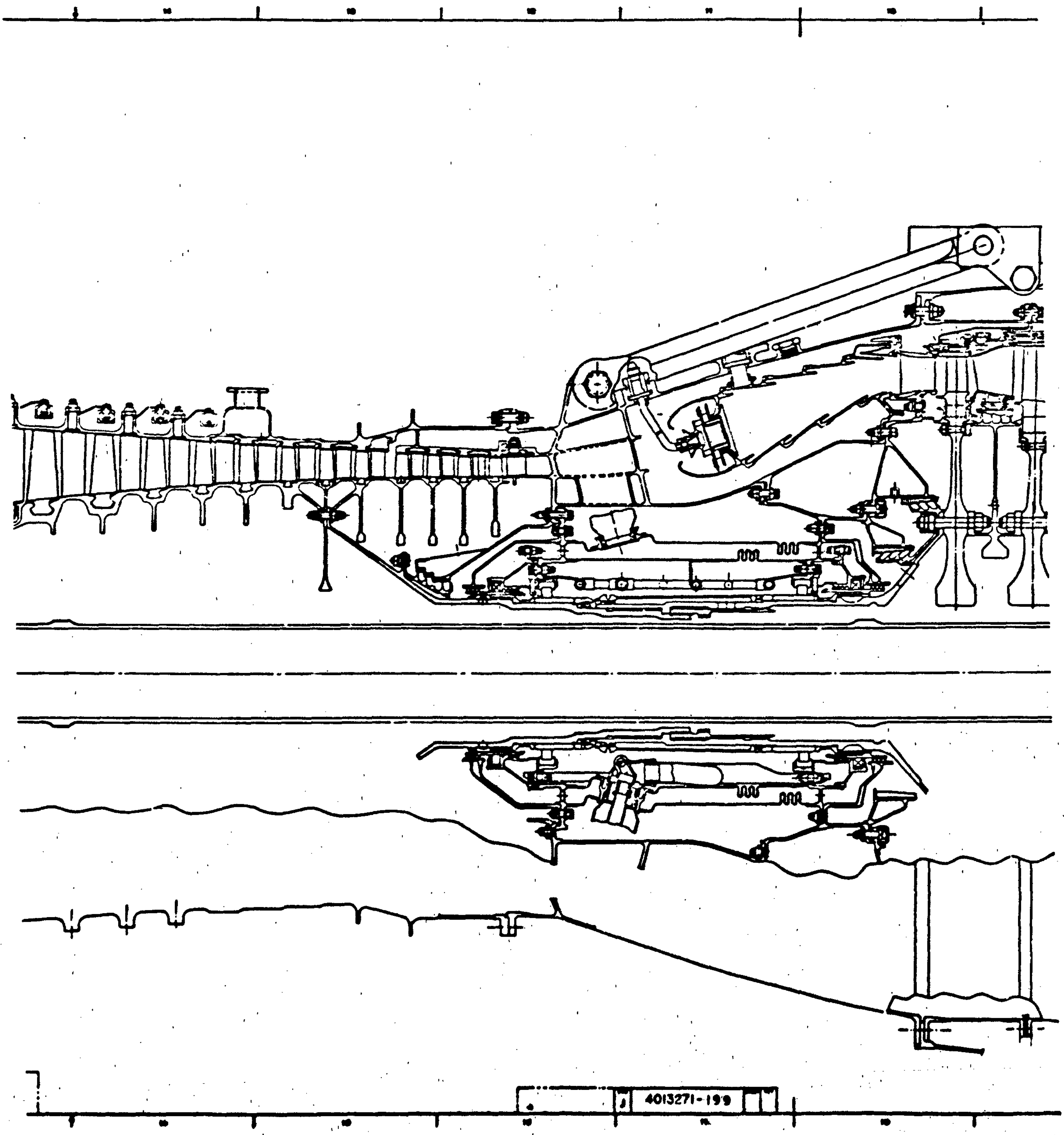

1 FAN FRAME MODIFIED TO SUPPORT STARTER/GENERATOR HOUSING (F8)
2 FAN ROLLER BEARING MOVED FORWARD 3.25 (D8)
3 EXTENDED LPT SHAFT FORWARD 3.90 (D7)
4 NEW FAN BEARING SUPPORT (E8)
5 NEW STUB SHAFT (D8)
6 NO 3 BEARING MOVED FORWARD 2.25 (D6)
7 NEW NO 3 BEARING SHAFT (D7)
8 ALL ITEMS OF THE POWER TAKE-OFF SYSTEM WERE REMOVED (INCLUDING BEVEL GEARS, PTO HOUSING, GEARBOX, ETC) (D7)
LAYOUT - PROPOSAL
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER / GENERATOR
(ELECTRICAL DISCONNECT)
120 KVA CYLINDRICAL (TF34)
J
07482
4013271-257
4013271-257
4013271-257

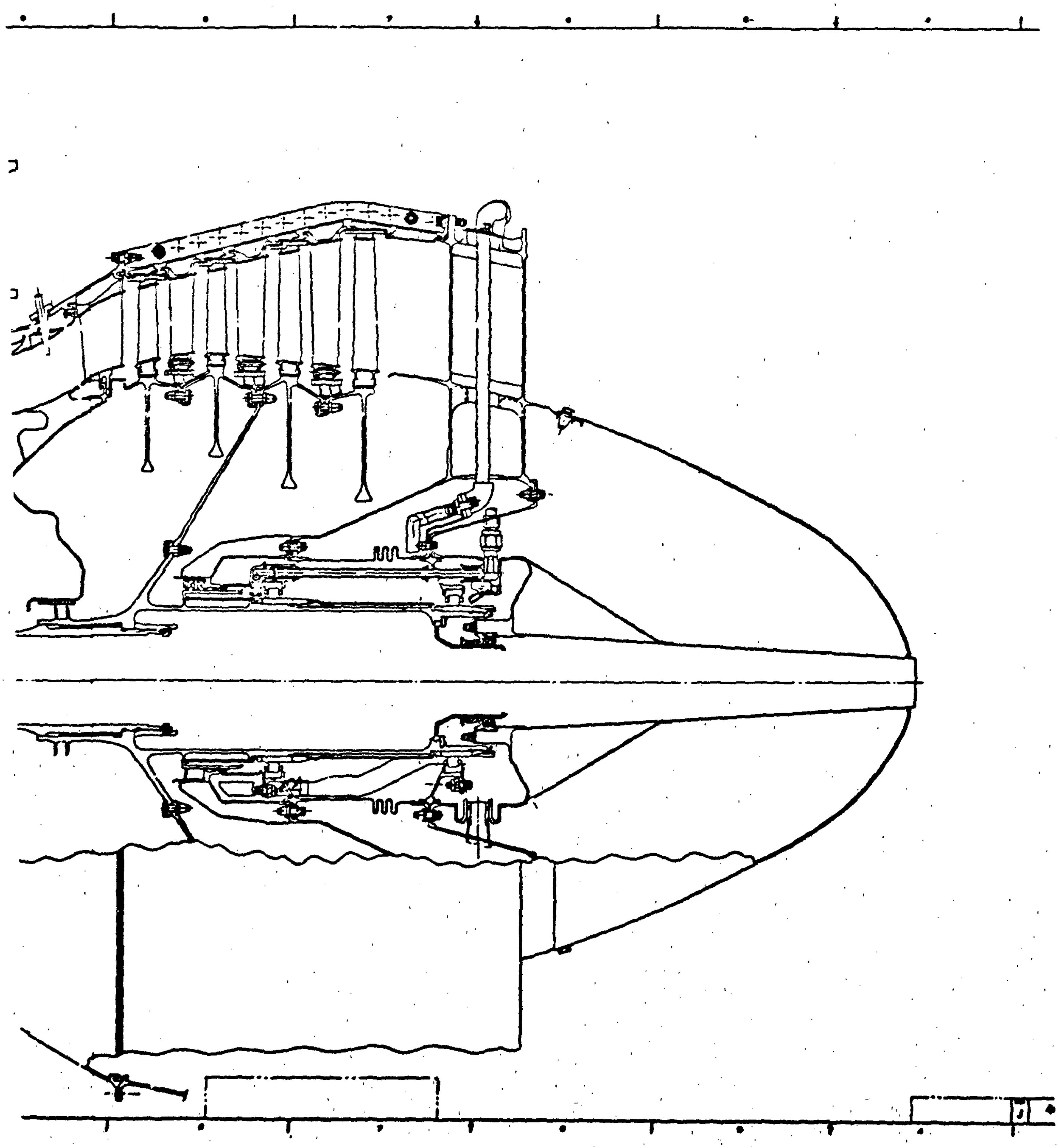


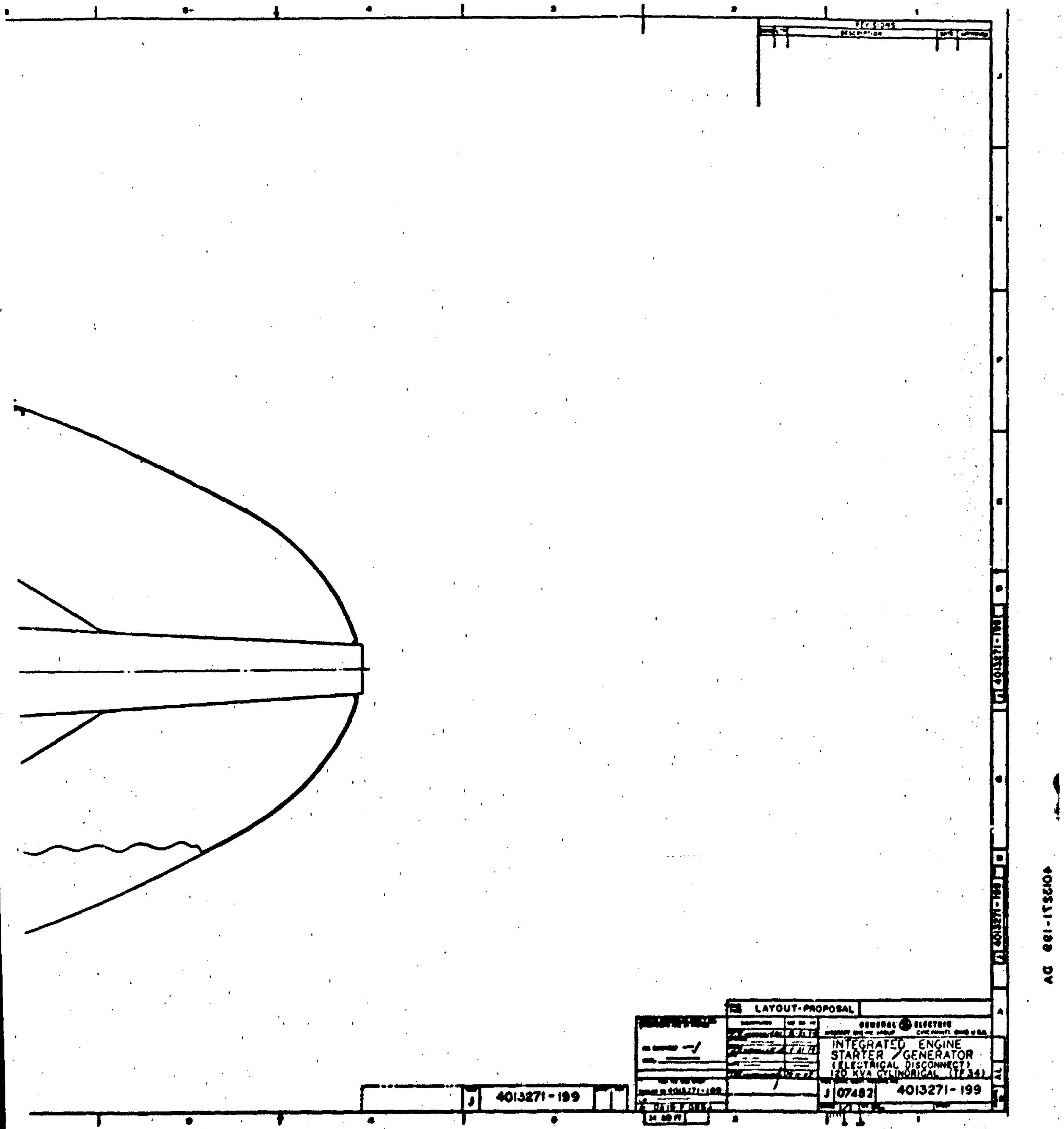
SECT B-B
VIEW A-A
CURRENT TRANSFORMER
FUSE

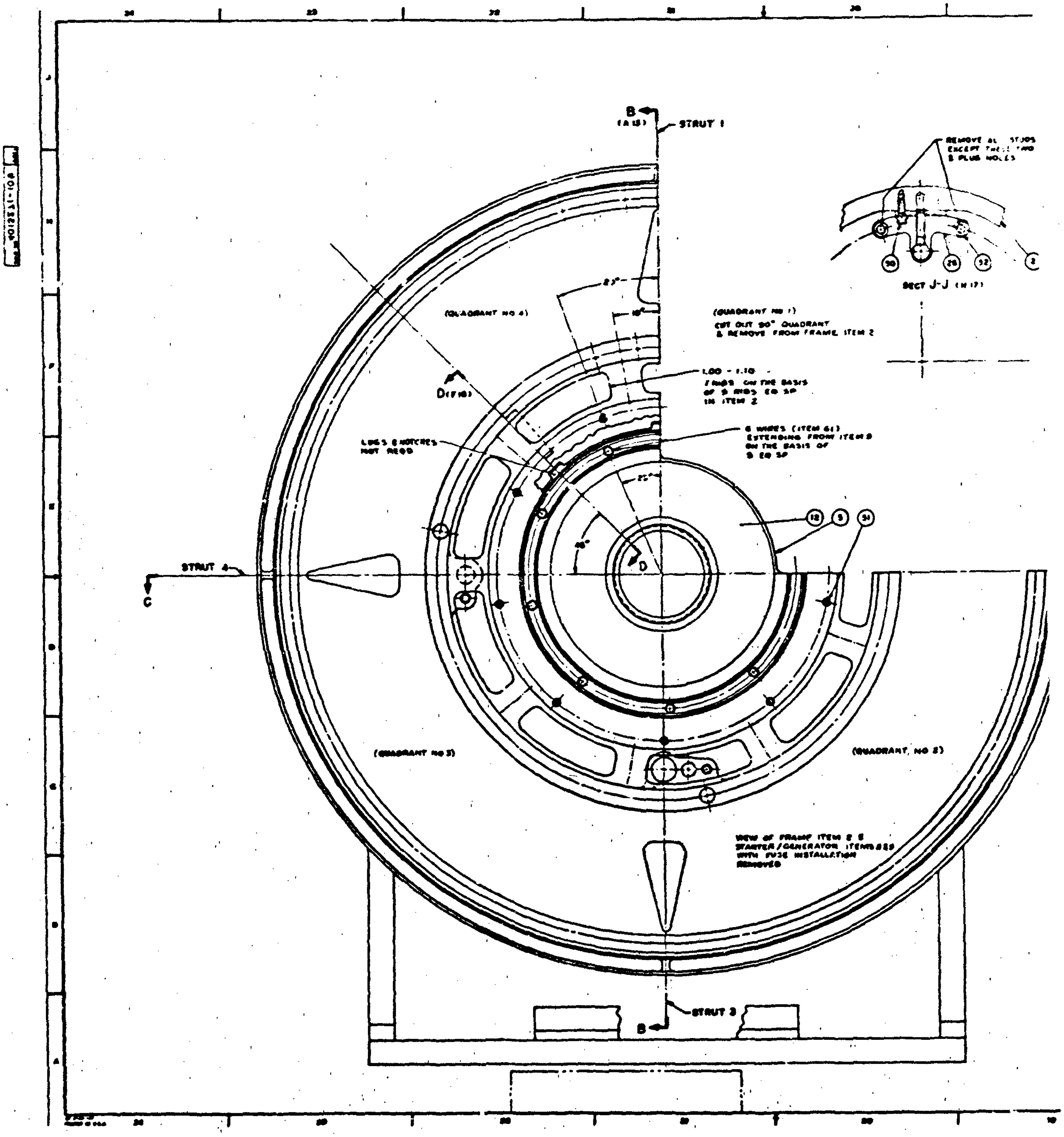

TOP VERT STRUT
A
B
TO ELECTRICAL CONNECTORS & FUSES
A
B
4013271-199

2

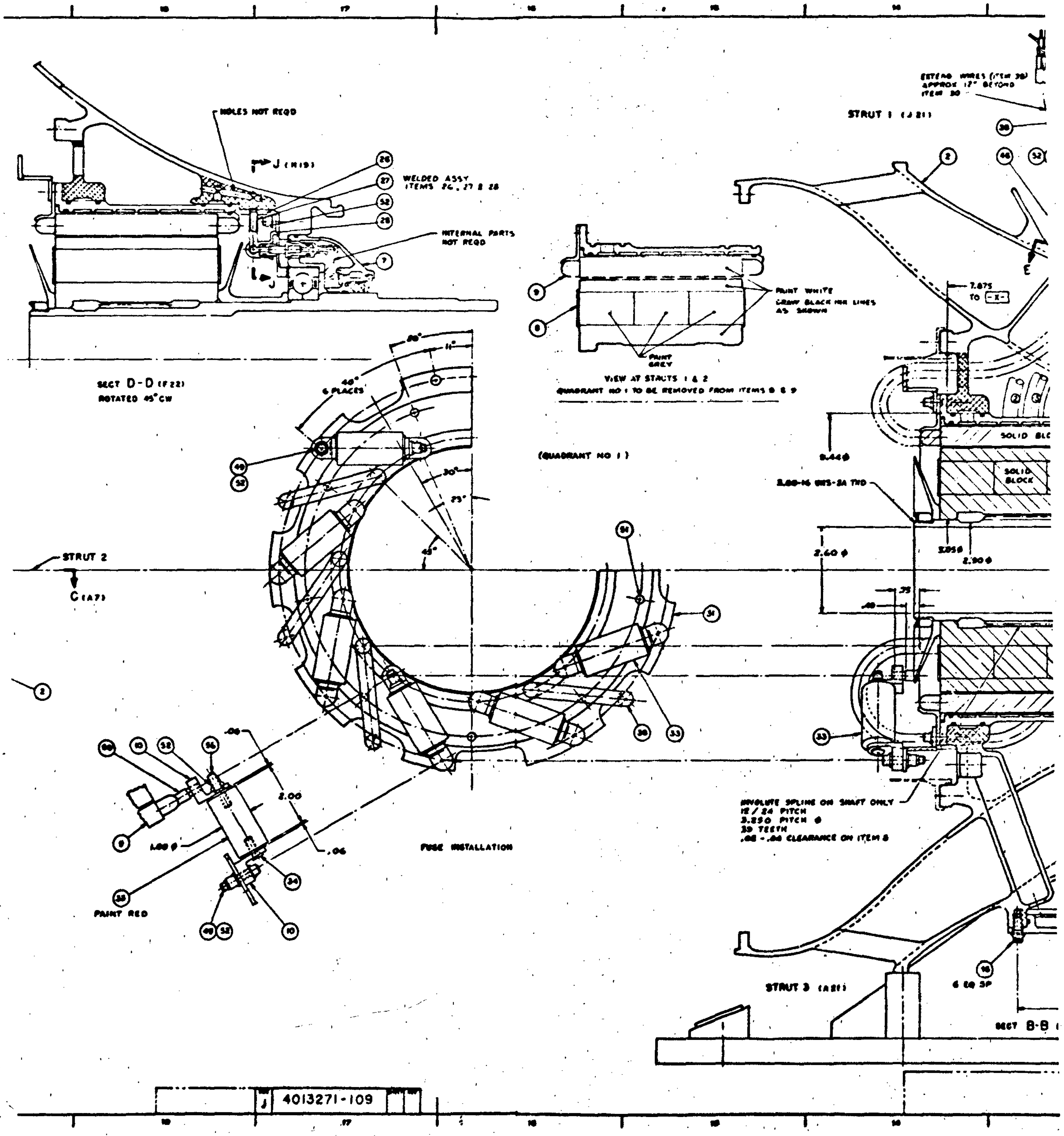

4013271-199



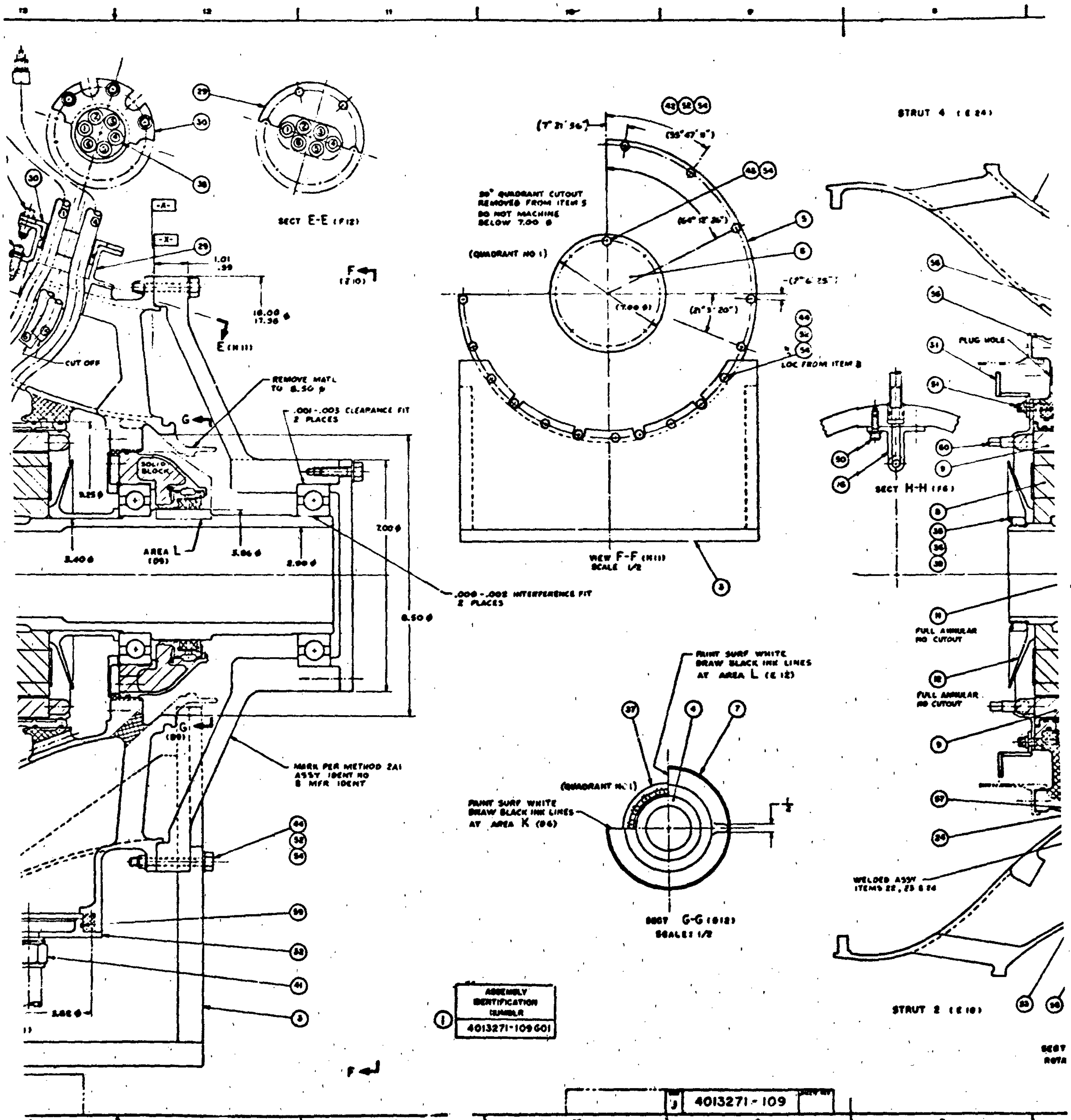

REVISIONS
DESCRIPTION
LAYOUT-PROPOSAL
GENERAL ELECTRIC
INTEGRATED ENGINE
STARTER /GENERATOR
(ELECTRICAL DISCONNECT)
120 KVA CYLINDRICAL (TF34)
J 07482 4013271-199
J 4013271-199
4013271-199 DV



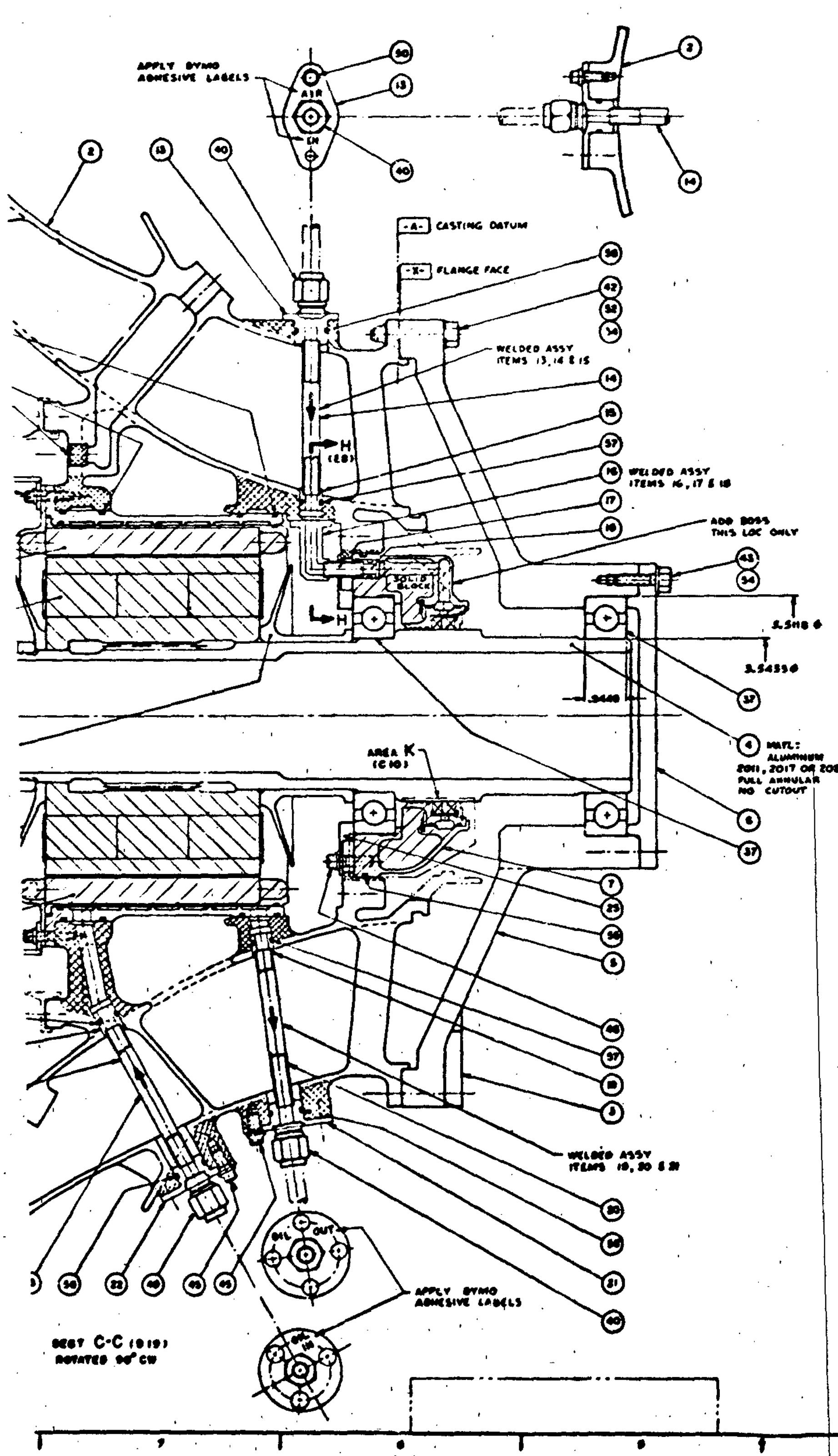
B
STRUT 1
REMOVE ALL STUDS EXCEPT THESE TWO & PLUG HOLES
SECT J-J
(QUADRANT NO 4)
(QUADRANT NO 1)
CUT OUT 90° QUADRANT & REMOVE FROM FRAME ITEM 2
1.00 - 1.10
6 WIRES (ITEM 61)
LUGS & NOTCHES NOT REQD
STRUT 4
C
D
(QUADRANT NO 3)
(QUADRANT NO 2)
VIEW OF FRAME ITEM 2 &
STARTER/GENERATOR
WITH FUSE INSTALLATION
REMOVED
STRUT 3
B



HOLES NOT REQD
WELDED ASSY ITEMS 26, 27 & 28
INTERNAL PARTS NOT REQD
SECT D-D (F22) ROTATED 45° CW
PAINT WHITE
PAINT GREY
VIEW AT STRUTS 1 & 2
(QUADRANT NO 1)
STRUT 1 (J21)
SOLID BLOCK
2.60 ⌀
STRUT 2
C (A7)
FUSE INSTALLATION
PAINT RED
2.00
.06
INVOLUTE SPLINE ON SHAFT ONLY
STRUT 3 (A21)
SECT B-B
J 4013271-109



SECT E-E (F12)
VIEW F-F (H11)
SCALE 1/2
30° QUADRANT CUTOUT REMOVED FROM ITEM 5 DO NOT MACHINE BELOW 7.00 Ø
(QUADRANT NO 1)
LOC FROM ITEM 8
STRUT 4 (E 24)
PLUG HOLE
SECT H-H (F6)
FULL ANNULAR NO CUTOUT
WELDED ASSY ITEMS 22, 23 & 24
STRUT 2 (E 10)
CUT OFF
REMOVE MATL TO 8.50 Ø
.001-.003 CLEARANCE FIT 2 PLACES
SOLID BLOCK
AREA L (D9)
.000-.002 INTERFERENCE FIT 2 PLACES
MARK PER METHOD 2A1 ASSY IDENT NO & MFR IDENT
PAINT SURF WHITE DRAW BLACK INK LINES AT AREA L (E 12)
PAINT SURF WHITE DRAW BLACK INK LINES AT AREA K (D6)
SECT G-G (D12)
SCALE: 1/2
ASSEMBLY IDENTIFICATION NUMBER
4013271-109G01
J 4013271-109



LIST OF MATERIAL

			MATERIAL			
	1	4013271-109 G01		ASSY	A 11	1
(A)	2	6030779 G01	ALUMINUM	FRAME ASSY	J 7	1
	3	4013271-129 G01	——	CRADLE, SUPPORT	D 9	1
	4	4013271-103 P01	SEE ZONE D4	SHAFT	B 4	1
	5	P02	HARD WOOD	FRAME, BRG SUPPORT	C 5	1
	6	P03		PLATE, BRG RETAINER	D 5	1
	7	P04		HOUSING, SEAL	D 5	1
	8	P05		STARTER / GENERATOR ROTOR	E 8	1
	9	P06		STARTER / GENERATOR HOUSING	E 8	1
	10	P07	BRASS	LUG, CONNECTOR	B17	12
	11	P08	ALUMINUM	OIL SLINGER, AFT	D 8	1
	12	P09		OIL SLINGER, FWD	D 8	1
	13	P10	SERIES 300 SST	FLANGE, AIR	J 7	1
	14	P11		TUBE .375 Ø	F 5	1
	15	P12		SLEEVE, AIR	F 5	1
	16	P13		INLET, AIR	F 5	1
	17	P14		TUBE .375 OD	F 5	1
	18	P15		SLEEVE, AIR	F 5	1
	19	P16		SLEEVE, OIL OUT	C 5	1
	20	P17		TUBE .375 Ø	B 5	1
	21	P18		FLANGE, OIL OUT	A 5	1
	22	P19		FLANGE, OIL IN	A 7	1
	23	P20		TUBE .37 Ø	A 8	1
	24	P21		SLEEVE, OIL IN	C 8	1
	25	P22		RETAINER, BRG	D 5	1
	26	P23		FLANGE, OIL IN	H17	1
	27	P24		TUBE .250 Ø	H17	1
	28	P25		BRACKET, SUPPORT	H17	1
	29	P26		HOUSING, SUPPORT	J12	1
	30	P27		HOUSING, WIRE	J12	1
	31	P28	SHEET STL	RING, FUSE SUPPORT	F 8	1
	32	P29	HARD WOOD	PLATE, COVER	B11	1
	33	P30		FUSE (SIMULATED)	C15	6
	34	P31	STL	.250-28 BOLT - SPECIAL	B17	6
	35	P32		LOCK NUT	E8	1
	36	P33		LOCK RING	E8	1
(B)	37	(3L18)	——	BALL BRG	E5	2
(E)	38	COML	COPPER	ELECTRICAL CABLE	H [illegible]	6
(D)	39	RR 325	STL	RING, RETAINING	E 8	1
	40	COML		NUT, TUBE FITTING .375 Ø	J 7	3
	41			NUT, TUBE FITTING .750 Ø	B12	1
	42			.250-28 HEX HD CAP SCR × 1.75 LG	H 5	11
	43			.250-28 HEX HD CAP SCR × 1.00 LG	E 5	8
	44			.250-28 HEX HD CAP SCR × 2.25 LG	C12	5
(A)	45	J644 P030		.250-28 MACH BOLT × .375 LG	A 7	7
(A)	46	P050		.250-28 MACH BOLT × .500 LG	H15	8
(A)	47	P070		.250-28 MACH BOLT × .625 LG	H15	3
(A)	48	P090		.250-28 MACH BOLT × .750 LG	C 5	8
(A)	49	P130		.250-28 MACH BOLT × 1.000 LG	B18	6
(A)	50	J643 P050		.190-32 MACH BOLT × .500 LG	E 9	6
(A)	51	P090		.190-32 MACH BOLT × .750 LG	F 8	7
(A)	52	J626 P04		.250-28 NUT	C18	33
	53	COML		.250-28 STUD × 1.00 LG	C18	6
	54			.250 Ø PLAIN WASHER	H 5	24
(C)	55	2-168	NEOPRENE	O-RING .093 NOM × 7.237 ID	E5	1
(C)	56	2-175		O-RING .093 NOM × 8.987 ID	H8	4
(C)	57	2-011		O-RING .062 NOM × .301 ID	F5	3
(C)	58	2-015		O-RING .062 NOM × .551 ID	H5	3
(C)	59	2-151		O-RING .093 NOM × 2.987 ID	B12	1
	60	COML	COPPER	WIRE .250 Ø × 1.12 LG INSULATED	E8	6

(A) SUPPLIED BY GE ENGINEERING - EVENDALE

(B) NEW DEPARTURE (OR EQUIV)
DIVISION GENERAL MOTORS CORP
BRISTOL, CONN

(C) PARKER SEAL CO (OR EQUIV)
2360 PALUMBO DR
LEXINGTON, KY 40509

(D) RAMSEY CORPORATION
P.O. BOX 513
ST. LOUIS, MO 63166

(E) ELECTRICAL CABLE .400 Ø OR SMALLER
7 WIRE (INSULATED)

7 MACHINE EXISTING BOSSES
PROVIDE ROOM FOR MATERI

6 PAINT THE FOLLOWING PART
ITEMS 2,5,6,7,8,9,31 &

5 THESE SYMBOLS
WOOD OR LAMINATIONS.
AT ITEMS 7, 8, & 9

4 THIS SYMBOL DENOTES
MATERIAL TO BE ADDED.

3 ALL CYLINDRICAL PARTS TO
UNLESS OTHERWISE SPECIFIE

2. DIMENSIONS NOT SHOWN MAY

1 MUST CONFORM TO:
P1TF3 CL-A (INTERPRETATI
P1TF9 CL-A (MACHINED FE
P1TF10 CL-A (SHEET METAL
P2TF3 (IDENT MARKING)

PRELIMINARY DWG, DESIGN
TO BE REVIEWED PREVIOUS
TO USE FOR PRODUCTION

DECIMALS ±.01
ANGLES ± 30'
125/
AS NOTED

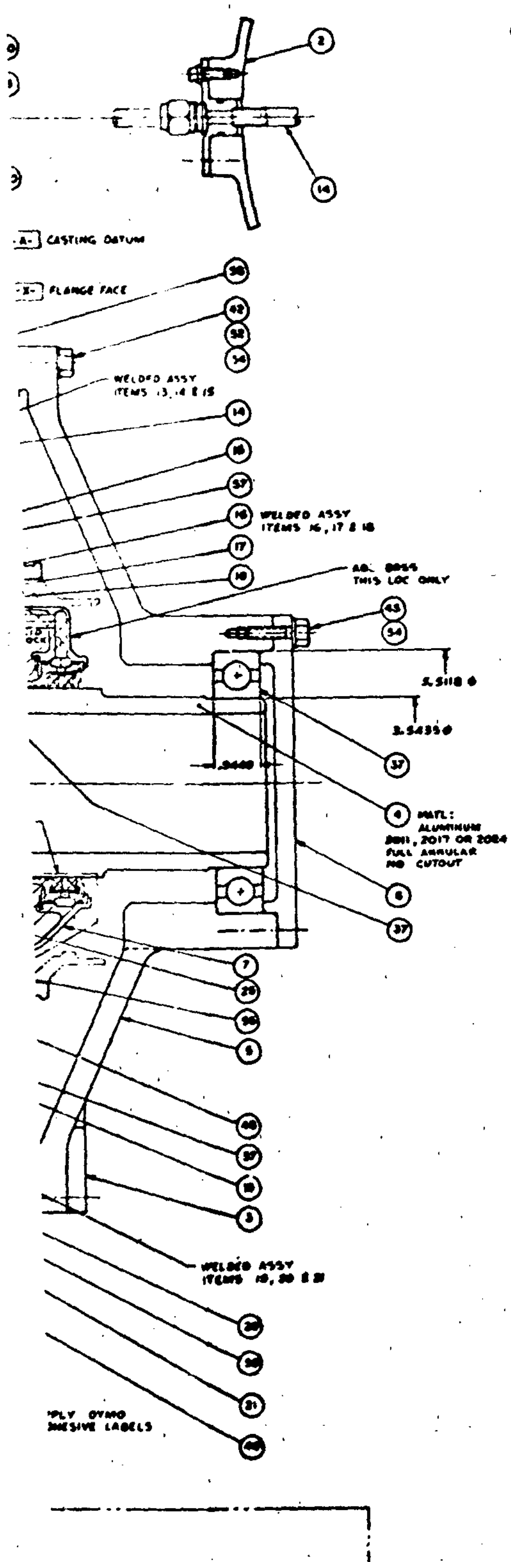
4013271-101

WORKING

F.R.VESTER

H.KOEHEL

J.M.MATTHEW



LIST OF MATERIAL

	ITEM NO	IDENTIFYING NO	MATERIAL	DESCRIPTION OR NAME	ZONE	QTY
	1	4013271-109 G01		ASSY	A11	X
(A)	2	6030T79 G01	ALUMINUM	FRAME ASSY	J7	1
	3	4013271-125 G01	——	CRADLE, SUPPORT	D9	1
	4	4013271-109 P01	SEE ZONE D4	SHAFT	B4	1
	5	P02	HARD WOOD	FRAME, BRG SUPPORT	C5	1
	6	P03		PLATE, BRG RETAINER	D5	1
	7	P04		HOUSING, SEAL	D6	1
	8	P05		STARTER / GENERATOR ROTOR	E8	1
	9	P06		STARTER / GENERATOR HOUSING	E8	1
	10	P07	BRASS	LUG, CONNECTOR	G17	12
	11	P08	ALUMINUM	OIL SLINGER, AFT	D8	1
	12	P09		OIL SLINGER, FWD	D8	1
	13	P10	SERIES 300 SST	FLANGE, AIR	J7	1
	14	P11		TUBE .375 Ø	F5	1
	15	P12		SLEEVE, AIR	F5	1
	16	P13		INLET, AIR	F5	1
	17	P14		TUBE .375 OD	F5	1
	18	P15		SLEEVE, AIR	F5	1
	19	P16		SLEEVE, OIL OUT	C5	1
	20	P17		TUBE .375 Ø	B5	1
	21	P18		FLANGE, OIL OUT	A5	1
	22	P19		FLANGE, OIL IN	A7	1
	23	P20		TUBE .375 Ø	A8	1
	24	P21		SLEEVE, OIL IN	C8	1
	25	P22		RETAINER, BRG	D5	1
	26	P23		FLANGE, OIL IN	H17	1
	27	P24		TUBE .250 Ø	H17	1
	28	P25		BRACKET, SUPPORT	H17	1
	29	P26		HOUSING, SUPPORT	J12	1
	30	P27		HOUSING, WIRE	J12	1
	31	P28	SHEET STL	RING, FUSE SUPPORT	F8	1
	32	P29	HARD WOOD	PLATE, COVER	B11	1
	33	P30		FUSE (SIMULATED)	C15	6
	34	P31	STL	.250-28 BOLT-SPECIAL	B17	6
	35	P32		LOCK NUT	E8	1
	36	P33		LOCK RING	E8	1
(B)	37	(3L18)	——	BALL BRG	E9	2
(E)	38	COML	COPPER	ELECTRICAL CABLE	[illegible]	6
(D)	39	RR 325	STL	RING, RETAINING	E8	1
	40	COML		NUT, TUBE FITTING .375 Ø	J7	5
	41			NUT, TUBE FITTING .750 Ø	B12	1
	42			.250-28 HEX HD CAP SCR X 1.75 LG	H5	11
	43			.250-28 HEX HD CAP SCR X 1.00 LG	E5	8
	44			.250-28 HEX HD CAP SCR X 2.25 LG	C12	5
(A)	45	J644 P030		.250-28 MACH BOLT X .375 LG	A7	7
(A)	46	P050		.250-28 MACH BOLT X .500 LG	H15	8
(A)	47	P070		.250-28 MACH BOLT X .625 LG	H13	5
(A)	48	P090		.250-28 MACH BOLT X .750 LG	C5	8
(A)	49	P130		.250-28 MACH BOLT X 1.000 LG	B18	6
(A)	50	J643 P050		.190-32 MACH BOLT X .500 LG	E9	4
(A)	51	P090		.190-32 MACH BOLT X .750 LG	F8	7
(A)	52	J626 P04		.250-28 NUT	C18	38
	53	COML		.250-28 STUD X 1.00 LG	C18	3
	54			.250 Ø PLAIN WASHER	H5	20
(C)	55	2-168	NEOPRENE	O-RING .093 NOM X 2.237 ID	C5	1
(C)	56	2-175		O-RING .093 NOM X 2.987 ID	H8	4
(C)	57	2-011		O-RING .062 NOM X .301 ID	F5	3
(C)	58	2-015		O-RING .062 NOM X .551 ID	H5	3
(C)	59	2-151		O-RING .093 NOM X 2.987 ID	B12	1
	60	COML	COPPER	WIRE .250 Ø X 1.12 LG INSULATED	E8	6

(A) SUPPLIED BY GE ENGINEERING - EVENDALE

(B) NEW DEPARTURE (OR EQUIV)
DIVISION GENERAL MOTORS CORP
BRISTOL, CONN

(C) PARKER SEAL CO (OR EQUIV)
2360 PALUMBO DR
LEXINGTON, KY 40509

(D) RAMSEY CORPORATION
P.O. BOX 513
ST. LOUIS, MO 63166

(E) ELECTRICAL CABLE .400 Ø OR SMALLER
7 WIRE (INSULATED)

7 MACHINE EXISTING BOSSES AT STRUTS 1 & 2 AS REQD TO PROVIDE ROOM FOR MATERIAL AS DESCRIBED IN NOTE 4

6 PAINT THE FOLLOWING PARTS WITH WHITE ENAMEL: ITEMS 2,5,6,7,8,9,31 & 32

5 THESE SYMBOLS DENOTE A SOLID BLOCK OF WOOD OR LAMINATIONS. DO NOT DRAW THE SYMBOL LINES AT ITEMS 7, 8, & 9

4 THIS SYMBOL DENOTES HARDWOOD OR OTHER SUITABLE MATERIAL TO BE ADDED. PAINT TO MATCH FRAME ITEM 2

3 ALL CYLINDRICAL PARTS TO HAVE .005-.010 CLEARANCE ON DIA UNLESS OTHERWISE SPECIFIED

2 DIMENSIONS NOT SHOWN MAY BE SCALED

1 MUST CONFORM TO:
P11F3 CL-A (INTERPRETATION OF DWG)
P11F9 CL-A (MACHINED FEATURES)
P11F10 CL-A (SHEET METAL PARTS)
P11F3 (IDENT MARKING) SEE ZONE C11

PRELIMINARY DWG, DESIGN TO BE REVIEWED PREVIOUS TO USE FOR PRODUCTION

DECIMALS ±.01
ANGLES ± 30'
125
AS NOTED

4013271-101

DATE 7-1975

WORKING LAYOUT

F.R. VESTER

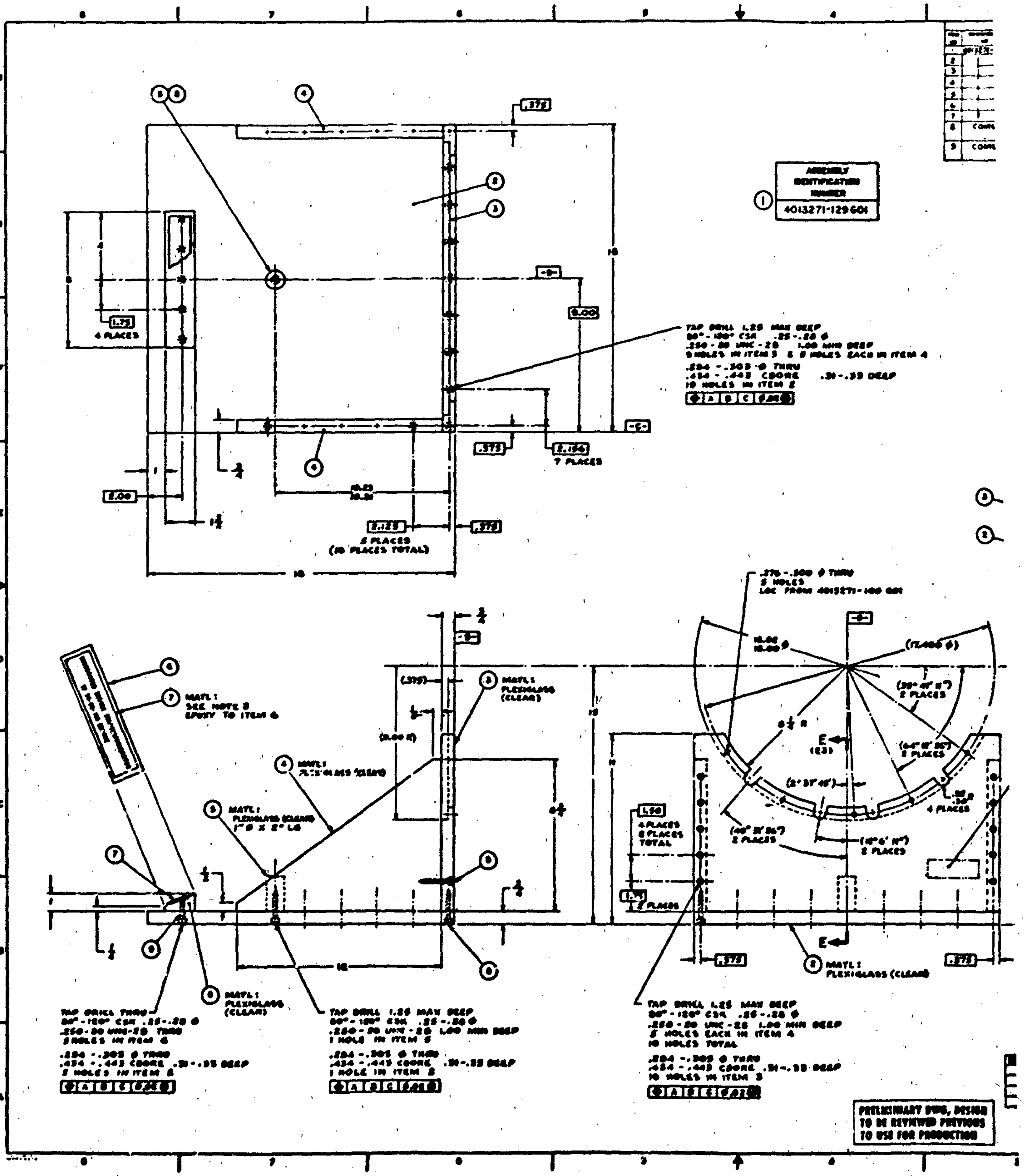
GENERAL ELECTRIC
AIRCRAFT ENGINE GROUP CINCINNATI, OHIO U.S.A.

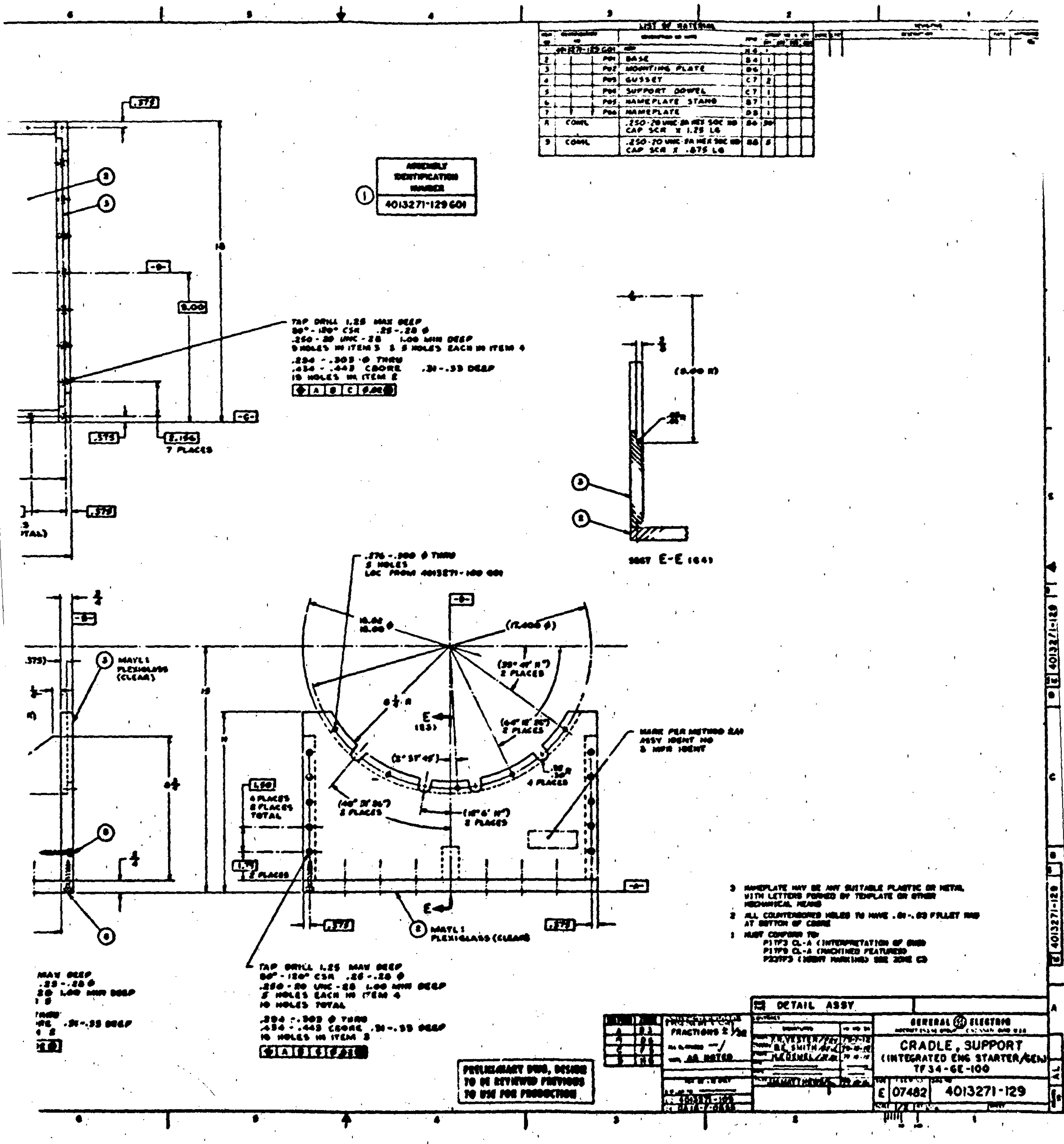
INTEGRATED ENGINE STARTER / GENERATOR (MOCK-UP TF34-GE-100)

J 07482 4013271-109

4013271-109

AO


ASSEMBLY IDENTIFICATION NUMBER
4013271-129601
MATL: PLEXIGLASS (CLEAR)
PRELIMINARY DWG, DESIGN TO BE REVIEWED PREVIOUS TO USE FOR PRODUCTION


LIST OF MATERIAL
4013271-129 G01
BASE
MOUNTING PLATE
GUSSET
SUPPORT DOWEL
NAMEPLATE STAND
NAMEPLATE
COML .250-20 UNC-2A HEX SOC HD CAP SCR X 1.25 LG
COML .250-20 UNC-2A HEX SOC HD CAP SCR X .875 LG
ASSEMBLY IDENTIFICATION NUMBER
4013271-129 G01
TAP DRILL 1.25 MAX DEEP
.250-20 UNC-2B 1.00 MIN DEEP
.294-.303 ⌀ THRU
.434-.443 CBORE .31-.33 DEEP
7 PLACES
SECT E-E
MATL: PLEXIGLASS (CLEAR)
2 PLACES
4 PLACES
MARK PER METHOD
ASSY IDENT NO
& MFR IDENT
TAP DRILL 1.25 MAX DEEP
.250-20 UNC-2B 1.00 MIN DEEP
5 HOLES EACH IN ITEM 4
10 HOLES TOTAL
.294-.303 ⌀ THRU
.434-.443 CBORE .31-.33 DEEP
10 HOLES IN ITEM 3
3 NAMEPLATE MAY BE ANY SUITABLE PLASTIC OR METAL WITH LETTERS FORMED BY TEMPLATE OR OTHER MECHANICAL MEANS
2 ALL COUNTERBORED HOLES TO HAVE .01-.03 FILLET RAD AT BOTTOM OF CBORE
1 MUST CONFORM TO:
P1TP3 CL-A (INTERPRETATION OF DWG)
P1TP9 CL-A (MACHINED FEATURES)
P22TP3 (IDENT MARKING) SEE ZONE C3
PRELIMINARY DWG, DESIGN TO BE REVIEWED PREVIOUS TO USE FOR PRODUCTION
FRACTIONS ± 1/32
AS NOTED
DETAIL ASSY
GENERAL ELECTRIC
CRADLE, SUPPORT
(INTEGRATED ENG STARTER/GEN)
TF34-GE-100
E 07482 4013271-129